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D-C Overpotential Testing Experience on High-Voltage Generators

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OVERPOTENTIAL tests are applied to electric apparatus to demonstrate the ability of the insulation to withstand a specific voltage for a certain period of time. These tests are based largely on experience and are established by industry standards for new machines. Practically all recognized standard overpotential tests on electric machinery are made with alternating current. During the last several years, investigation of d-c overpotential testing has indicated basic advantages over a-c testing and that direct current could be used as a satisfactory overpotential test on large high-voltage machines. From a practical standpoint, the overpotential test does not check the adequacy of the design or inherent breakdown level of the insulating materials, but rather is a check for flaws in the materials and manufacturing methods. The test which is effective in achieving this objective with the least destruction to the basic materials accordingly would have the greatest merit.

Basic Advantages of D-C Overpotential Testing

Recent investigations[1] have shown some significant advantages to be obtained using d-c overpotential tests:

1. The voltages which are equally searching for defects and physical damage are far less damaging than the equivalent alternating voltages.

2. The slope of the voltage endurance curve is such that the time of voltage application is not nearly so critical with direct as with alternating current.

3. The problems associated with d-c testing of large equipment are far simpler, as a testing device with limited capacity can be used. Therefore, the d-c tester is a small, relatively portable device which can utilize any convenient power supply.

4. The use of the d-c test voltages required will give assurance that the insulation has passed a voltage test which can be co-ordinated with conventional machine lightning arrester protection.

Experience in testing many specimens of conventional high-voltage insulation has given a basis for determining equivalent alternating and direct voltages to produce insulation failures. Figure 1 is illustrative of tests of this nature. Also, a-c and d-c breakdown tests on old stator windings served to help establish a ratio of direct to alternating current (rms) for equally searching effect for common types of insulation faults. It was found that an equally searching value of direct current is a smaller percentage of the inherent d-c strength of insulation than in the case with alternating current. On the basis of these investigations, a value of direct to alternating current (rms) of 1.6 has been proposed for maintenance tests of large high-voltage rotating machinery.[2]

Figure 1 indicates the results of both a-c and d-c dielectric strength studies of high-voltage insulation samples. The flatness of the voltage endurance curve for direct current is of considerable interest as it indicates that time of direct voltage application is far less significant in its effect on failure voltage than when a-c stress is applied. It further indicates that there is no fixed ratio of direct to alternating voltage for equal destructive effect, and that if a d-c to a-c ratio is to be employed it must be at a specific time of voltage application

For small apparatus, which has low capacitance from the winding to ground, a-c test equipment is readily portable as the size is not important. On larger and higher voltage apparatus, where the capacitance of the winding to ground is large, the test equipment for overpotential testing with alternating current becomes quite large and testing, both in the shop and in the field, presents some problems. On the other hand, d-c overpotential test equipment can be made which is reasonably small and portable. Figure 2 shows a commercial instrument suitable for making high-voltage d-c overpotential tests on generator windings. There is considerable interest in d-c overpotential testing[2] because of its convenience and the information obtained from current leakage measurements. A-c overpotential testing usually involves no current measurement and, therefore, must be destructive to detect faults. On the other hand, d-c testing makes leakage current measurements easy. Furthermore, the leakage current is not masked by charging current as is the case with a-c testing.

Previous Limitations of D-C Testing

D-c overpotential testing of insulation is by no means a new subject. However, in recent years there has been a renewed interest in this controversial subject because of new information presented by various investigators. Early investigations with d-c testing listed the following disadvantages:

1. Different stress distribution with direct current than with alternating current.

2. Lack of suitable high-voltage d-c test equipment.

3. The wide variation of direct to alternating voltage breakdown of solid insulations.

It has been a usual practice in testing that the stress applied during test should be as similar as possible to the stress to which the equipment is subjected in normal service. In general, therefore, a-c apparatus should be tested with alternating current. However, present tests of d-c equipment are made with alternating current. A main objection to d-c testing is that the stress distribution of a-c apparatus is determined by the leakage resistance when direct current is applied and by the capacitance when alternating current is applied. The stress distribution with a d-c test therefore may differ greatly from that in normal service. It is true that the stress with direct current extends much farther out on end winding portions than an equivalent a-c stress. This results in parts of the winding being subjected to stresses different from operating conditions. However, the stress with direct current appears to build up slow enough so that it does not constitute a major problem with the usual times that such tests are applied. In several cases, defective end-turn insulation has been discovered at direct voltages far below the peak of regular a-c tests that could not be detected by alternating current. For factory testing, it is expected that d-c tests will show up poor insulation design on parts formerly of little concern. During a-c tests on solid insulation, the stress depends upon the

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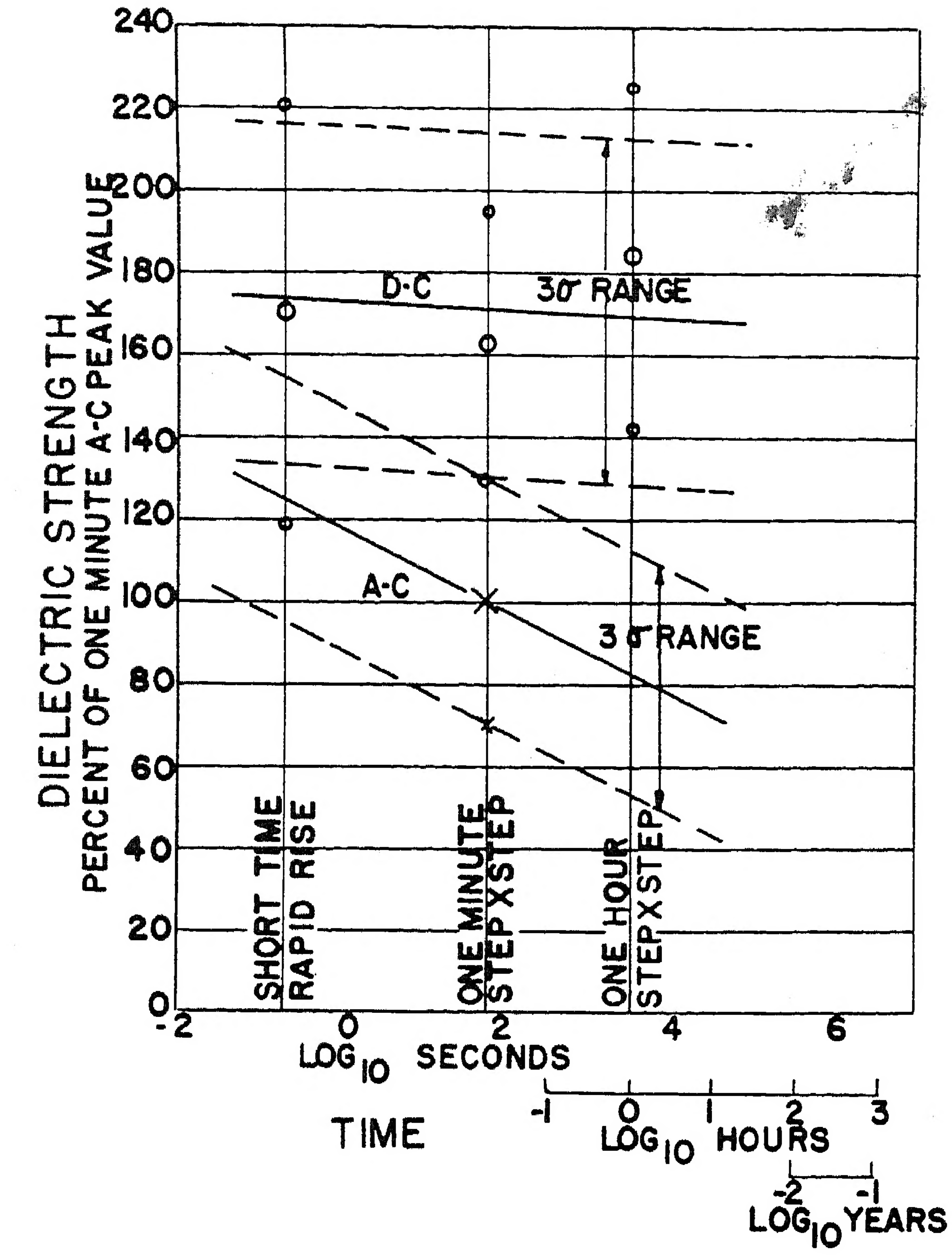

Figure 1 (left). Alternating and direct voltage endurance for high-voltage generator insulation. Large symbols indicate averages; small symbols indicate 3σ limits

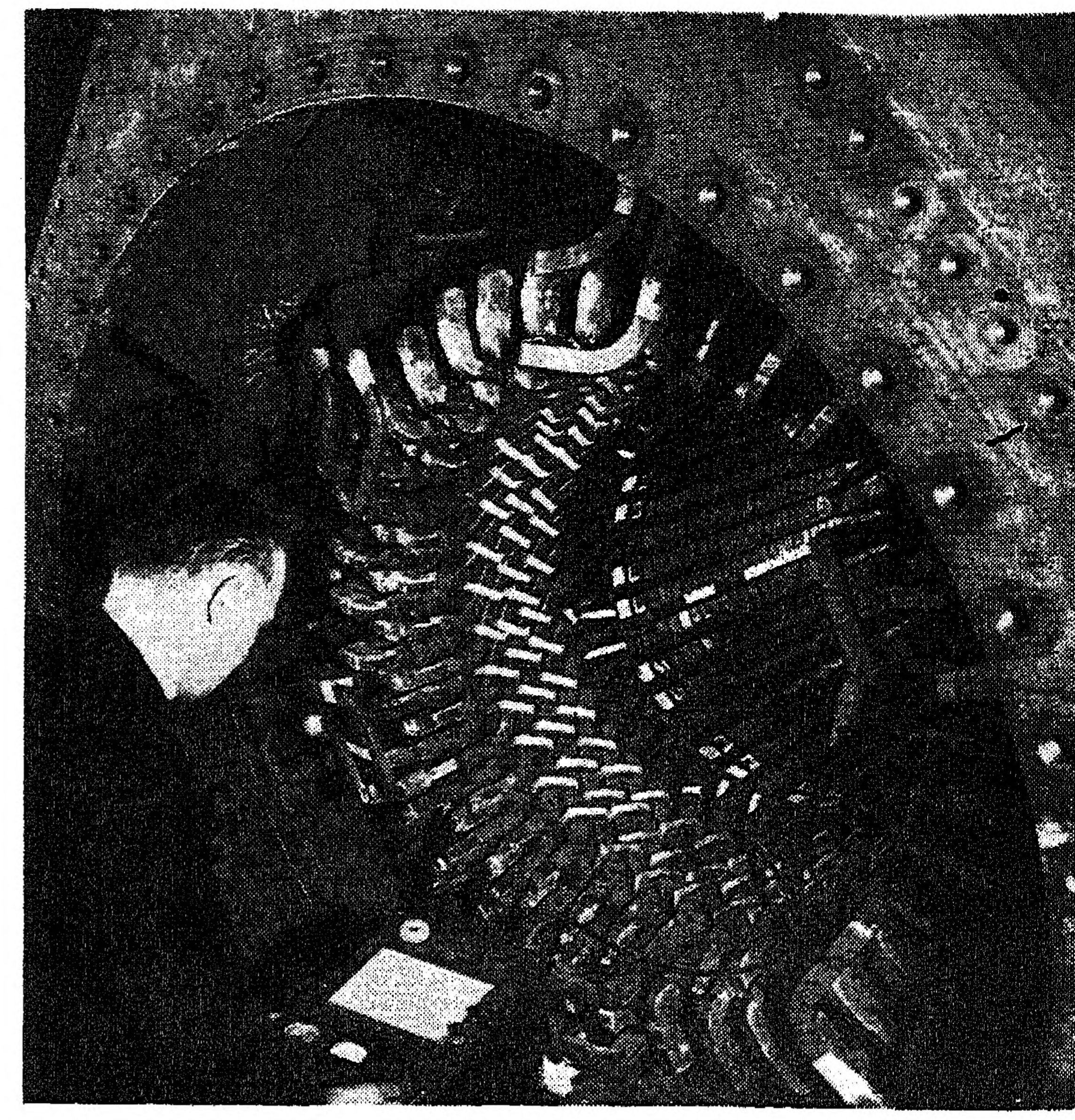

Figure 2 (right). D-c overpotential test of a large turbine generator

thickness and dielectric constant of the components, thus stressing the binding materials more than the mica because of the lower dielectric constant of usual binding materials. With d-c tests, the stress distribution through the material will be determined by the volume resistivity of its components and the major components of the insulation regarding dielectric strength (the mica) will be subjected to the greatest stress.

Even early investigations were quick to note advantages of applying a direct voltage which was considerably less than the normal operating alternating voltage. The majority of insulation failures in service are due to the presence of moisture, which gives rise to conducting paths on the internal or external surfaces of the insulation. A d-c test is a very sensitive detector of moisture penetration. Insulation resistance measurements have provided a relatively simple criterion of the condition of insulation for many years.

Recent improvements in the last decade in high-voltage rectifying tubes have largely eliminated the problem of equipment. Several manufacturers are able to supply d-c test apparatus with the voltage range and portability required. Three conventional circuits for d-c test equipment are shown in Figure 3. These testers usually use one of two basic circuits with slight modifications.

1. Single high-voltage rectifier circuits—full or half wave.

2. Lower voltage rectifiers which use ladder circuits in cascade to produce the required output voltage.

Adequate d-c insulation test equipment utilizing each method is in use.

The variation in the d-c and a-c peak breakdown voltages of solid insulations is a very controversial subject and probably will prevent utilizing the advantages of d-c testing until more information on specific insulation systems can be determined. It is doubtful that an absolute equivalent ratio of d-c to a-c peak voltage can be determined for solid insulation. However, and this is important, d-c tests can be used to give assurance of the absence of specific types of insulation faults.

Shop D-C Overpotential Testing

The advantages of d-c testing were applied to d-c armature coils long before its use was extended to high-voltage insulation. In connection with turn insulation failures on a certain type motor, d-c testing was distinctly advantageous in the detection of copper slivers and similar faults. D-c testing of turn insulation on low-voltage equipment during coil manufacture has given excellent results during the past 4 years.

Preliminary winding tests have been made with direct as well as with alternating current on high-voltage windings for the last year. Manufacturers' preliminary winding tests are directed toward weeding out defective insulation that may result in coil failures during final test. A d-c test voltage is required that has equal or greater destructive effect than the final 1-minute a-c test voltage to adequately insure that any coils with weak insulation are detected before the final test. Table I indicates some of the test voltages that are applied during winding of high-voltage machines. At these high direct voltages, little attempt has been made to correlate leakage and dielectric strength; the primary use of the leakage is an indication to the tester of impending trouble indicated by current runaway. Another factor preventing correlation between leakage and insulation strength during d-c test is that momentary or 1-minute tests are used and no steady-state leakage is reached in these time intervals; the rate at which the voltage is applied is an im-

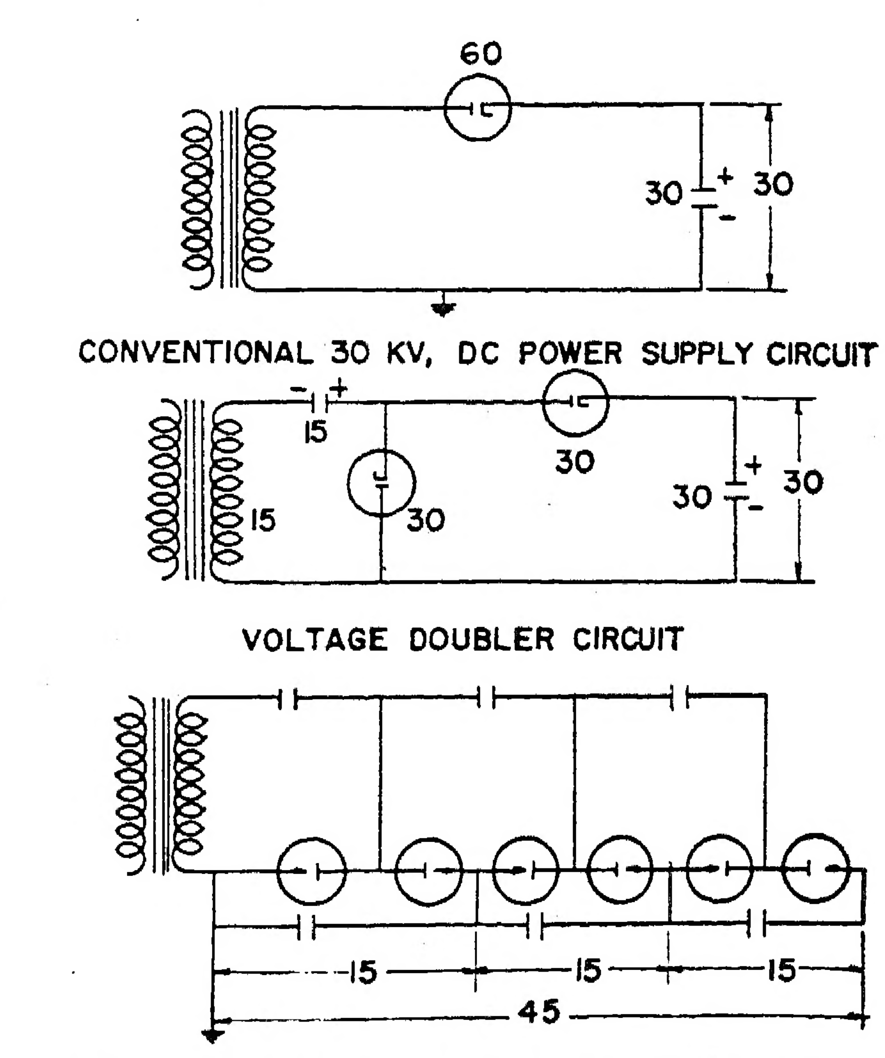

Figure 3. Conventional d-c high-voltage test circuits

Table I. D-C Overpotential Test Voltages

Voltage Class, Kilovolts	Bottom Coil Test (Momentary), Kilovolts	First Winding Test (Momentary Before Connections), Kilovolts	Second Winding Test (Momentary After Connections), Kilovolts	Final Test, Kilovolts
11	59	59	56.5	51.5
12.5	66	66	63.1	57.4
13.2	71	71	67.5	61.4
13.8	73	73	69.3	63.0
14.4	77	77	73.3	66.7
16.5	89	89	83.8	76.2
18	97	97	91.3	83.0

portant factor which determines the leakage at these relatively short time intervals.

For adequate d-c test voltages for preliminary winding tests of the conventional 13.8-kv class high-voltage stators, a tester of about 75 kv is required. With the advent of higher direct voltages applied to high-voltage windings, a new problem became more serious than with the lower voltage testers. Voltage recovery of solid dielectrics is due to the absorptive nature of such dielectrics. The reversible absorption current which causes voltage recovery is a function of the applied voltage, time of voltage application, and type of dielectric. Voltage recovery characteristics of several high-voltage windings have been measured. It is evident that recovery voltages of sufficiently high magnitudes as to constitute a serious safety problem were possible if the winding was not grounded properly after a high-voltage d-c test. Figure 5 shows the voltage recovery characteristics of a high-voltage winding with different grounding times. A winding should be grounded at least 60 minutes after a high-voltage d-c test has been applied.

Shop acceptance of d-c overpotential testing has been good. This can be attributed to several factors:

1. Ease of operation of test equipment.
2. Reduced setup time.
3. Visual indication of insulation condition during testing by suitable meters on the test equipment.

Results of using d-c overpotential tests during winding of high-voltage generators have shown d-c tests can be relied upon to locate and detect incipient insulation weaknesses in high-voltage generator insulation. It is expected that in the near future d-c will replace preliminary a-c tests in the winding sections. This will eliminate all a-c tests except the commercial final winding test.

In addition to the final a-c overpotential tests on new windings, dielectric absorption curves at operating stress are contemplated. This information will be available to the customer and may be used as an aid in determining insulation deterioration during subsequent inspections. It is hoped also that leakage current at some direct voltage that will be used for overpotential testing the winding during maintenance periods can be furnished the customer.

D-C Maintenance Tests of Large High-Voltage Generators

Field acceptance and maintenance tests have long been a problem because of the size of a-c test equipment and power supply required. Overpotential tests are the only means for providing assurance that the winding insulation has a certain minimum insulation level. Suitably selected values of direct current are equally as searching for likely types of insulation weakness with the expectancy of small or negligible destructive effect on good insulation. In addition, d-c overpotentials appear to be fundamentally advantageous for maintenance testing. In addition to basic advantages, the re-

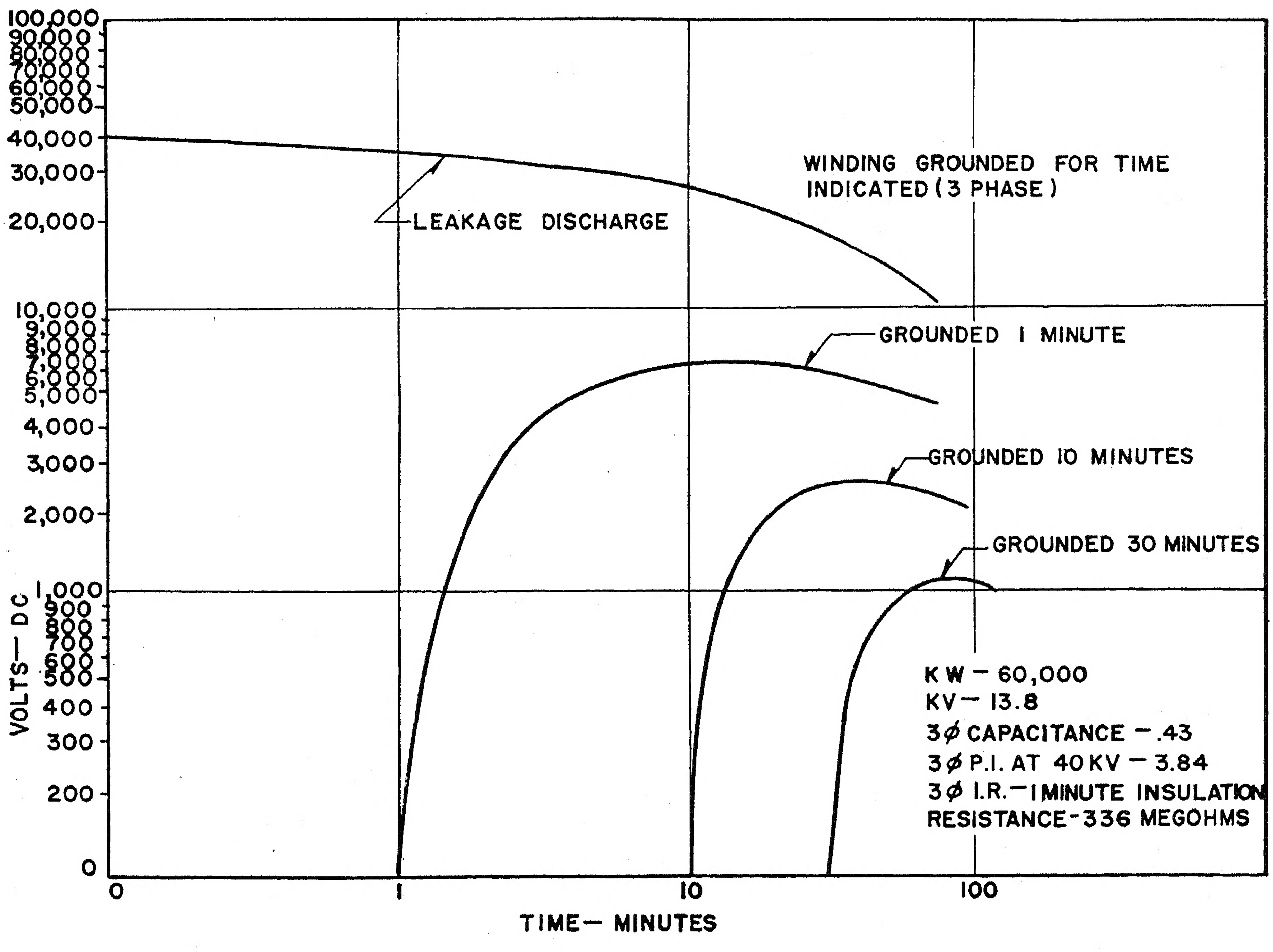

Figure 4. Voltage recovery characteristics of a high-voltage generator winding

quired test equipment is much less expensive and far less costly and cumbersome to transport. Also, insulation leakage at high stresses may be measured. Therefore d-c overpotential tests were recommended for maintenance tests.

During the last 2 years, many generators have had d-c overpotential maintenance tests applied to the stator windings. For 13.8-kv machines with modern insulation, the usual d-c overpotential test was 30 kv for 1 minute. In no case has this test failed to pick out serious cases of insulation weaknesses. In addition to stator insulation faults, other incipient weaknesses have been detected in buswork and cables. During maintenance d-c overpotential tests, 10-minute dielectric absorption curves are made in the range of 1 to 15 kv direct current. Overpotential tests usually have been made on all three phases to ground with direct current; separating of phases usually involves considerable labor and time and is not considered necessary unless trouble develops. On windings indicating low insulation resistance or failure on overpotential test, phases are separated to assist in locating the trouble. However, most cases of winding faults during overpotential tests are readily visually detected. The ability of direct current to detect and locate insulation faults with minimum damage to the insulation is now well established for maintenance testing.

In several instances d-c tests have indicated very low insulation resistance. One such case involved a thermocouple in the neutral which, with the winding isolated from ground, caused very high leakage at relatively low d-c test voltage. This thermocouple, not used since the first few days of operation, was removed and the machine had a satisfactory insulation resistance at 30 kv direct current. In another instance, a generator cable passing near a concrete wall had been sprayed with aluminum paint, which possibly could cause trouble if not removed. The d-c test quickly indicated high leakage. In only one case has a high-voltage machine indicated high leakage during d-c test which could be attributed to the machine insulation.

The usual range of insulation resistance after 1-minute voltage application during d-c overpotential test (25 to 30 kv direct current) encountered in high-voltage synchronous machine windings is as follows:

Hydrogen-cooled synchronous generators, 100 to 500 megohms.

Air-cooled synchronous generators, 50 to 200 megohms.

These values are average values for many machines tested and do not necessarily indicate that an insulation resistance lower than these limits should cause concern. Insulation resistance, especially at high direct voltages, depends upon many factors. For an individual generator winding, such factors as type of insulation, age, atmospheric conditions, cleanliness, and size of winding must be taken into account when considering insulation resistance of a particular machine.

Summary

To date about 4,000,000 kva of high-voltage generating equipment have been inspected in the field and d-c overpotential tests have been made to give assurance of adequate insulation level. About 1,000,000 kva of generating equipment have been tested with direct current in the Westinghouse factory. The success and general acceptance of this test method has been widespread both in the shop and in the field.

While much fundamental investigation of d-c versus a-c testing will be accomplished by many individuals in the next few years, there can be little doubt that d-c overpotential testing will be expanded where applicable to take advantage, in addition to fundamental advantages, of its lower initial cost and lower operating cost, with more informative test results.

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No Discussion

Rapid Measurement of the Thermal Resistivity of Soil

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Synopsis: Following a method due originally to Stalhane and Pyk,[1] the thermal resistivity of soil may be measured in situ by thrusting a long heated needle into the ground to the required depth and measuring its temperature every 30 seconds for 15 to 30 minutes. The slope of the resulting temperature versus time curve on semilog paper is proportional to the thermal resistivity of the soil, and the constant of proportionality is derivable theoretically. The finite diameter of the needle is taken into account by adjusting the zero time to a value found by plotting $dt/d\theta$ against t. The effects of the finite length of the needle are difficult to assess but appear to be unimportant for the duration of a typical determination. Resistivity values obtained agree with those obtained by the steady-state method. A crew of three can make determinations at the rate of one to three per hour.

THE effect of the thermal resistivity of the soil on the temperature rise of a buried cable is well known. It is of particular importance for the newer directly buried and pipe-type installations in which the temperature rise through the soil forms a larger part of the total rise than is the case with the older concrete duct types.

In the autumn of 1949, plans of The Hydro-Electric Power Commission of Ontario to install some 25,000 feet of underground high-voltage cable necessitated the immediate determination of the thermal resistivity of the soil along the proposed route with a reasonable degree of accuracy.

There are two possible procedures to be followed in making such measurements. Samples of the soil may be conveyed to the laboratory or the measurements may

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be made *in situ*. Since the thermal resistivity of the soil is very dependent upon the degree of compaction and the moisture content and since these are bound to suffer change while the sample is being dug up and conveyed to the laboratory, an *in situ* method is greatly to be preferred.

Laboratory Methods

The classical laboratory procedure is to use a hot plate with a guard ring. This is an absolute method in that it measures the thermal resistivity directly in terms of its definition as the temperature drop per unit thickness per unit rate of heat flow per unit area. For homogeneous, simple substances, this device provides the basic method of thermal resistivity measurement against which all other methods may be compared. For complex, heterogeneous materials such as soil, however, the picture is somewhat different. The large thermal gradient involved in the usual hot-plate determination, and the long times required to reach equilibrium, cause the moisture in the soil to migrate to the cool plate. The resulting variations in moisture content are bound to give erroneous results. Also, the apparatus required for this method is very costly and must be used with great care to obtain significant results. Obviously then, it is not suited to the investigation of the thermal resistivity of the soil along a proposed cable route.

Classical *in situ* Method

Any method designed to measure the thermal resistivity of an undisturbed sample of soil must employ some buried, heated body and must provide for the measurement of the temperature rise of this body, or of an adjacent measuring point, over the temperature of some remote point. The buried, heated body may be either a cylinder or a sphere, but the latter is the more usual choice as it avoids the troublesome end effects. The very act of burying such a body results in some disturbance of the soil, but this can be overcome by careful recompaction and by allowing time for the soil to regain its original conditions before measurements are made. The heated body must be supplied with a heat inflow which must be maintained constant until equilibrium conditions are reached. This may take many days.

The buried-sphere method was used at five positions along the proposed cable route and values of thermal resistivity from 52 to 860 thermal ohm-centimeters (degrees Celsius-centimeters per watt) were obtained. This abnormally large value turned out to be due to a bed of broken porcelain insulators. However, a number of actual soil resistivities went well over 100 thermal ohm-centimeters. The large range of these results indicated the need for a more detailed survey of the route. This was clearly impractical with the buried spheres because of the long times involved and especially because the storage batteries supplying them must be changed daily.

The type of sphere used for these steady-state measurements may be of interest. Those described in the literature[2] consist of an air-filled, hollow metal sphere containing a heater which is carefully designed and expensively constructed to produce an approximately isothermal surface. This type of construction was clearly impractical for the large number of determinations to be made in a short time. To achieve an isothermal surface inexpensively so that a large number of spheres could be constructed and installed at reasonable cost, a solid, cast-aluminum-alloy sphere was developed. It was made approximately 4 inches in diameter with a $1^1/_4$-inch diameter hole $2^1/_2$ inches long cast into it. This hole was plugged with an aluminum cylinder on which was wound a heater. A thermocouple was embedded in the outer surface of the sphere on its equator. Laboratory tests indicated that the outer surface was isothermal to within a small fraction of a degree Celsius.

Transient Methods

While searching for a more rapid method of making these thermal measurements, it was discovered that F. C. Hooper of the University of Toronto had in his possession copies of some European papers[1,3,4] which described a rapid, transient method of measuring thermal resistivity and which gave the mathematical derivation of the necessary equations. This is based on a suggestion made originally by Stalhane and Pyk.[1]

This transient method is based on the fact that not only the ultimate temperature rise of the heated body but also the rate of temperature rise will depend upon the thermal constants of the material in which it is immersed. Whereas it is usual to use a sphere for the steady-state method to avoid the end effects, the transient method can avoid these simply by using a time short enough to prevent them becoming important. The use of a cylinder in the form of a long needle has the advantage that it may be buried simply by thrusting it into the soil to the required depth. The most difficult thing about this method is the mathematical basis. However, the practical calculations are simple and may be reduced to an easy routine which can be followed successfully without need for mathematical training.

This transient needle method was used successfully and checked experimentally against the steady-state, buried-sphere method during the fall and winter of 1949–1950. Further laboratory experiments are planned to check the needle operation under a range of controlled soil conditions and to answer certain questions about sample size, accuracy, and moisture migration. In addition, further theoretical work is being carried out in an attempt to put the measurements on a less empirical basis. The Chemical Research Department of The Hydro-Electric Power Commission of Ontario also is using the transient principle, but with a much smaller needle, to measure the resistivities of thermal insulating materials.

Construction of Needle

All the needles built to date by the present authors have used a 1/4-inch outside-diameter steel tube as the outer cover. Since this is fairly flexible, it is necessary to make the heater assembly flexible also. A convenient way of doing this and at the same time obtaining an electrical resistance which can be matched conveniently by storage batteries is to wind number 26 gauge enameled, double-cotton-covered copper wire on a length of varnished cambric or polyethylene tubing which is then pulled into the steel tube. The lower end of the heater wire is soldered to the steel tube which acts as one lead. The lower end of the tube is plugged with a pointed steel rod to assist in inserting the needle into the soil. The upper end of the tube has a handle brazed to it to facilitate its removal after the test.

When constructing the earlier needles, great care was taken to ensure that the thermocouple was in contact with the steel tube so that the temperature would be measured at a definite radius and at a point outside the actual source of heat. Further experience, however, indicated that it was quite as good and much more convenient to put the thermocouple inside the hollow heater coil form, and so avoid any heater asymmetries and risks of short circuits.

These needles use a heater at least 2 feet long. The latest one has a total heater length of 5 feet with four number 30, Copper-Constantan thermocouples,

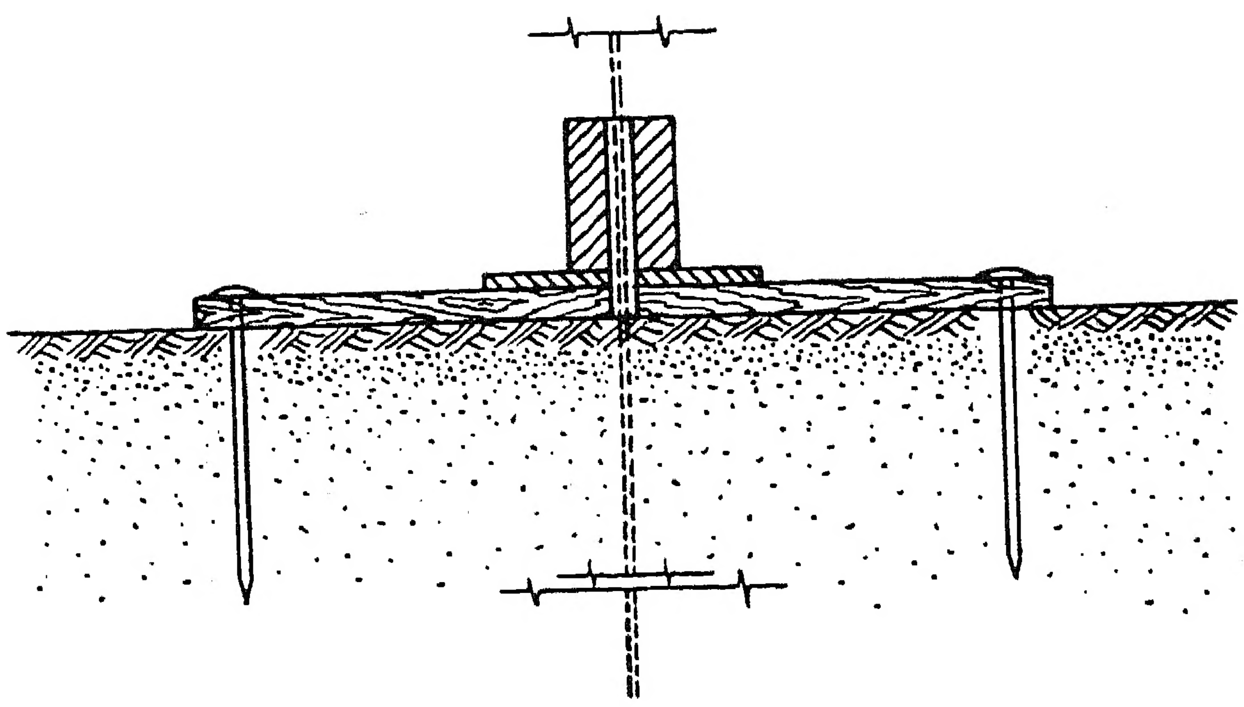

Figure 1. Guide plate for auger

placed at 1, 2, 3, and 4 feet from the end of the needle so that each test gives four values for the thermal resistivity at four different depths.

The leads from the heater and the thermocouples are brought out to convenient sockets mounted on the handle, and connecting cords are furnished to allow of easy connection to the instruments.

If insulated resistance wire with a negligible temperature coefficient and a convenient resistivity were available, it could be used with advantage for the heater as it would then be necessary to measure only the current input rather than the power as at present.

Method of Use

For most soils it is advisable to prepare a hole by using a solid steel rod with an auger welded to its end. To avoid enlarging the hole near the surface, a plate, see Figure 1, is used to guide the rod.

Once the needle is in place, the temperatures of the thermocouples are read at intervals until they have reached the temperature of the earth as shown by no further change in the temperatures indicated. This usually takes about 10 minutes. The power is then turned on and is maintained at a constant value by the use of a variable resistor and a wattmeter. A second operator takes readings of the temperatures indicated by the thermocouples at exactly 30-second intervals from the instant of switching on. They are taken for approximately 25 minutes unless the indicated temperatures reach dangerously high values (of the order of 100 degrees Celsius) earlier. If the thermal resistivity is abnormally high, it may be necessary to allow the needle to cool to ambient earth temperature, and then repeat the run with a reduced power input. For most soils, a power input of 15 to 25 watts per foot of needle is satisfactory.

A crew of three men with two needles can make one to three resistivity determinations per hour. The third man goes on ahead and buries the second needle so that it has reached equilibrium with the soil by the time the other two get to it.

The temperature readings obtained are plotted on the linear scale of a sheet of semilog paper against the times from the instant of switching on the power. A typical curve obtained forms Figure 2. Neglecting the readings for the first 3 to 5 minutes, it is usually possible to draw a straight line through the rest of the points so plotted. The thermal resistivity is measured by reading off the rise in temperature (in degrees Celsius) over one cycle of the logarithmic scale, and multiplying this by the factor $4\pi L/2.303\ p$, where p is the power input to the needle in watts, and L is the length of the needle in centimeters. This gives the thermal resistivity of the soil in thermal ohm-centimeters (degrees Celsius-centimeters per watt). For a needle with a 2-foot-long heater, this needle constant reduces to $332/p$.

It is tempting to simplify the measurement procedure still further by taking only two spot readings at say 5 and 10 minutes after switching on. If this is done, curves can be prepared so that the thermal resistivity is read off directly, or a simple slide-rule calculation can be used. Experience has indicated, however, that this is not sufficiently reliable. If it is worth making the measurement, it is worth taking readings every 30 seconds and plotting the temperature versus log-time curve. Any unusual behavior then shows up, and grossly inaccurate results caused by one incorrect reading can be avoided.

For the usual run of soils, a plot of the temperature rise θ versus log t for times of 5 to 25 minutes gives an obviously straight portion. For some soils, particularly for those with high resistivity, there may be no straight portion. In such cases, the assumption that the time t can be measured from the instant of switching on must be discarded for a more accurate one. As will be shown in detail, this corrected time t' is measured from a time $-t_0$ so that $t'=t+t_0$. This artificial zero time $-t_0$ may be determined by plotting a curve of $dt/d\theta$ versus t and extending the straight portion of this

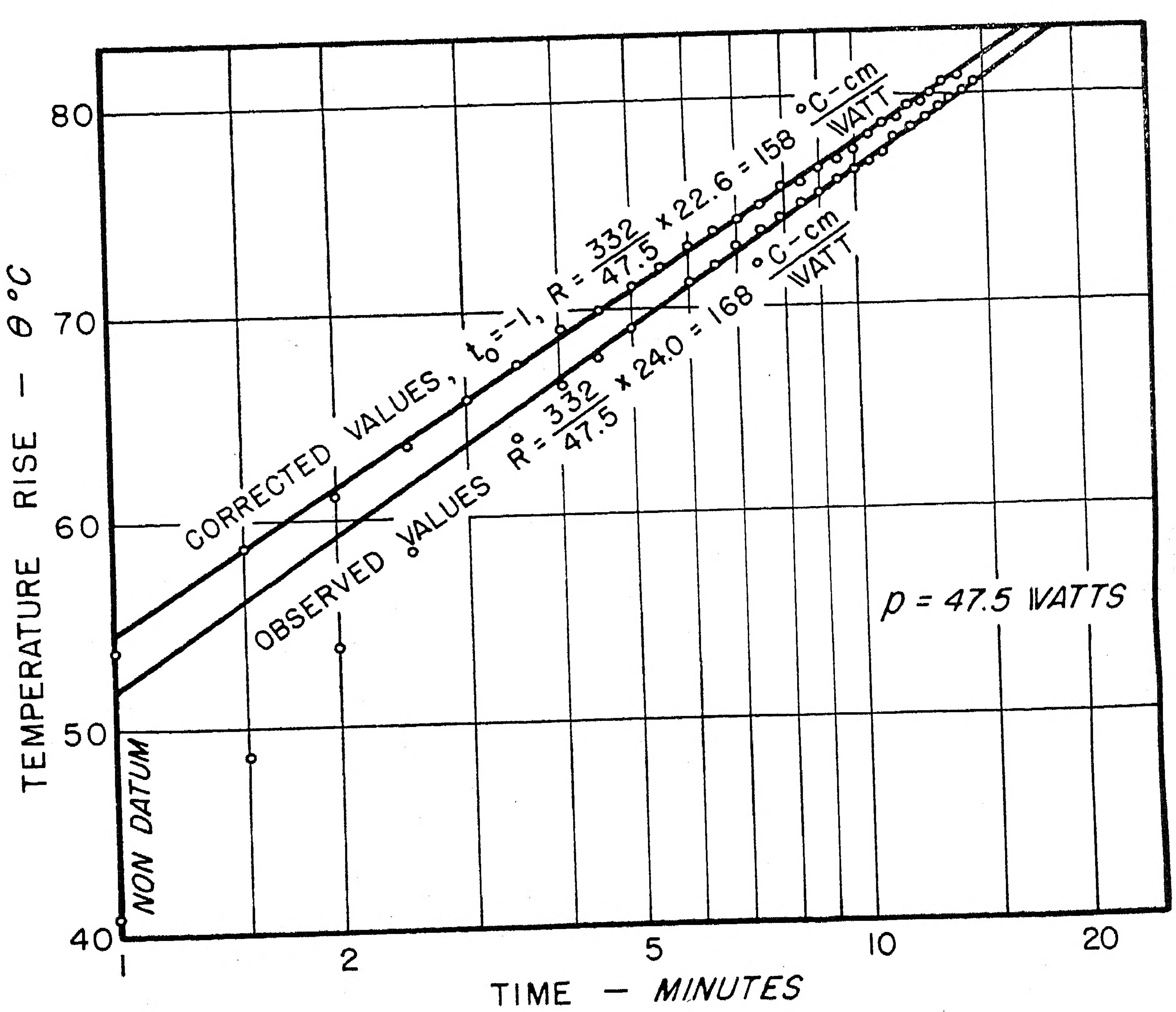

Figure 2. A typical curve of temperature rise versus time on semilog paper

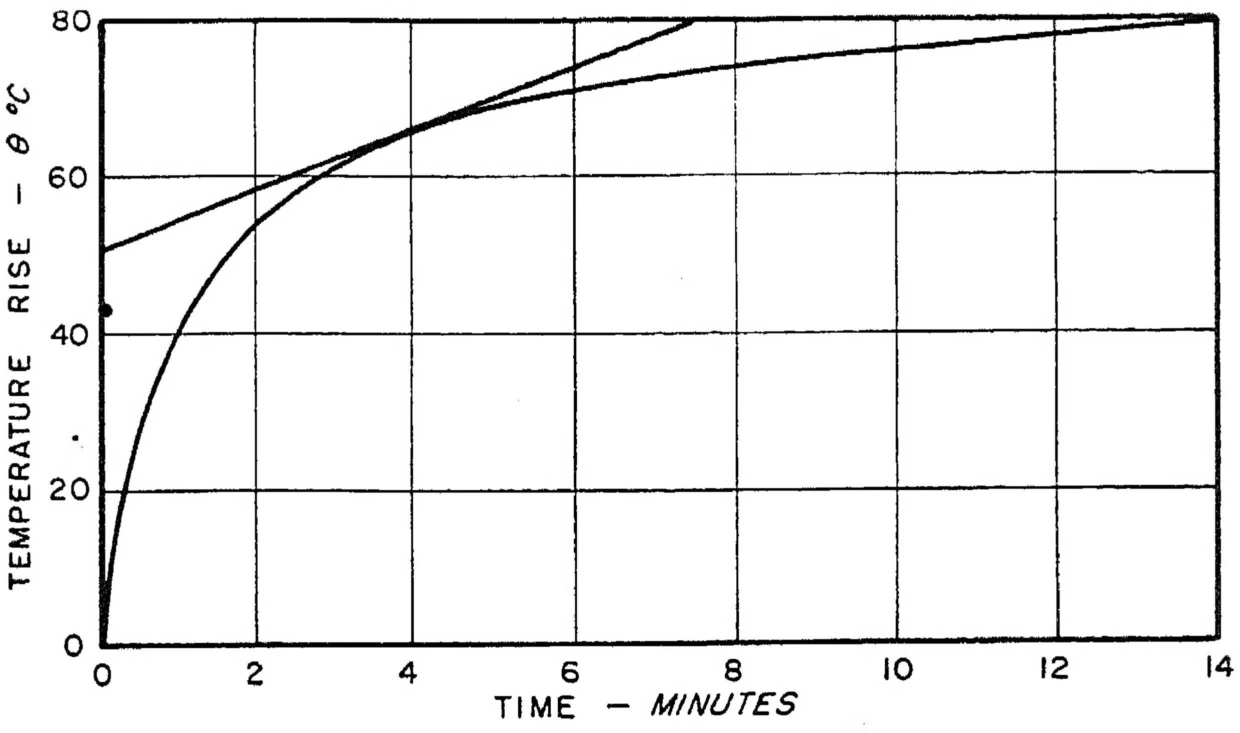

Figure 3 (left). Uncorrected curve of Figure 2 on linear time scale, showing one tangent at t=4

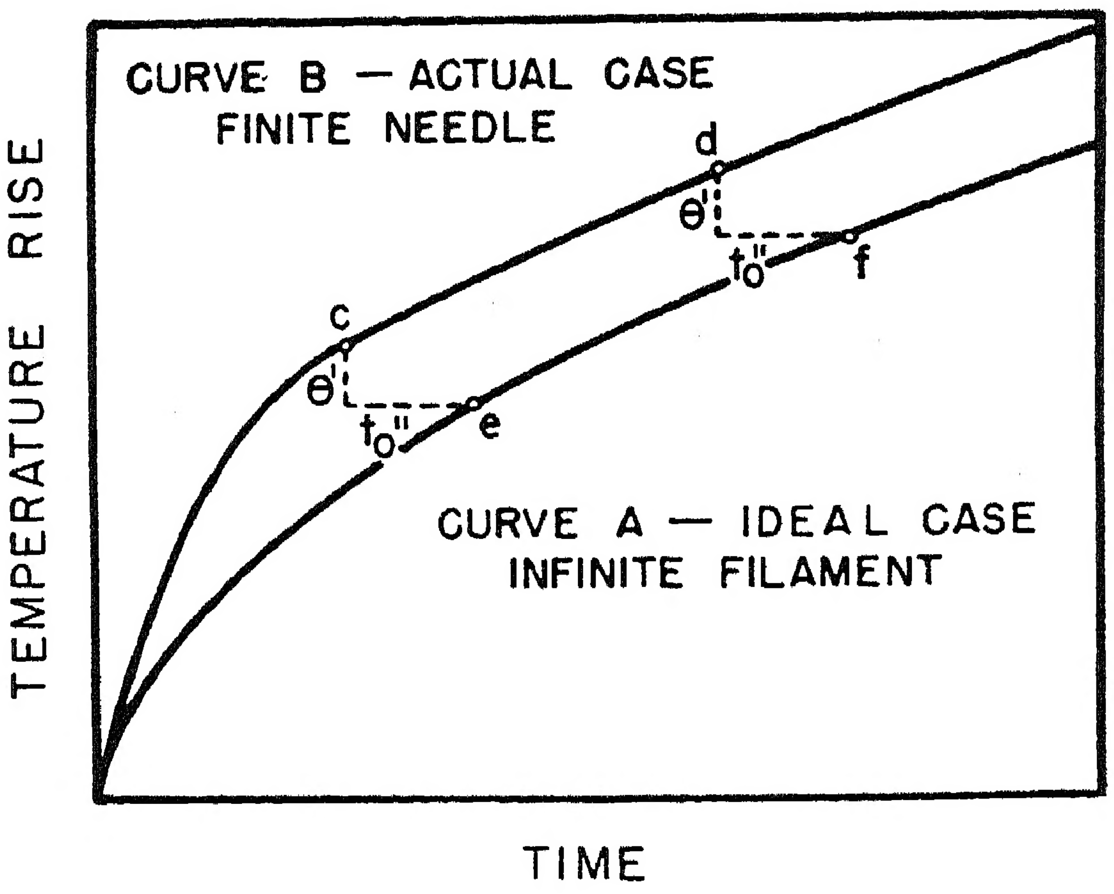

Figure 5 (right). Temperature rise versus time at a distance r

curve to cut the time axis at $-t_0$. Figure 4 is such a plot for the data of Figure 2. To obtain the values of $dt/d\theta$, it is necessary to plot a curve of θ versus t on linear paper, see Figure 3, and then to draw a number of tangents. Unfortunately, this is a rather laborious process and the method of drawing tangents yields a large scatter of the points on the $dt/d\theta$ versus t curve.

The corrected times t' obtained by adding t_0 to each value of t are then plotted on semilog paper against θ. As shown in Figure 2, this yields a straight line, the slope of which gives a considerably more accurate value of R than the original plot of θ versus log t. Usually this corrected value will be slightly lower than the uncorrected one. The values found for t_0, with this type of apparatus normally range up to 3 minutes.

The slope of the $dt/d\theta$ curve may also be used to obtain a value of R as described later. This has the theoretical advantage over the use of the slope of the θ versus log t curve in that a lower value of t is allowable without correction. Due to the scattering of the $dt/d\theta$ values, however, the slope of this curve is sometimes rather difficult to obtain. Nevertheless, it does give a check on the value obtained from the θ versus log t curve.

Accuracy

Comparison of the thermal resistivity values measured by the transient needle method with those obtained by the steady-state buried-sphere method show agreement to within the accuracy of measurement with either method.

The accuracies obtainable depend to some extent upon the time which can be spent plotting the points. If the apparent zero time t_0 found from the $dt/d\theta$ curve is used to plot a corrected curve of θ versus log t', the points so obtained will usually lie on a very good straight line, the slope of which may be measured to three figures. For most soils, however, sufficient accuracy is obtainable from the initial plot of θ versus log t. The value of resistivity obtained from this initial plot is usually high by a few per cent, but this is a small error compared to the variation of the resistivity with the seasons due to the changing moisture content.

Theory of Transient Method

The long heated cylinder or needle, 1/4 inch in diameter and 2 to 5* feet long, used for measuring soil resistivity is approached mathematically by considering the temperature rise θ at a distance r from an infinitely long, infinitely thin, heated filament supplied with a constant heat inflow of q watts per centimeter length ($q=p/L$). As is shown in Appendix I, the solution for this involves an exponential integral of the form $\int_x^\infty (\epsilon^{-x}/x)dx$. The European literature kindly made available by Mr. Hooper deals with this by using an approximation valid for sufficiently small distances r and sufficiently long times t.

Within these limits on r and t, the temperature rise is given by the approximate relation

$$\theta = ARq \log_{10} t + B \qquad (1)$$

where A and B are constants. This approximate equation for θ can be rendered more accurate by measuring the time from an instant prior to the actual switching on, that is, by adding a constant t_0' to the measured value of time t before plotting a curve of θ versus log t. As is proved in Appendix I, t_0' can be evaluated by plotting a curve of $dt/d\theta$ versus t and finding the t-axis intercept of the straight portion. The intercept will be at the point $-t_0'$ so long as the requirements of an infinitely long, infinitely thin filament in an infinite medium are met. As will be shown, for this apparatus, t_0' is usually small compared to t_0'', the correction for the finite diameter of the needle. The method of obtaining this zero correction actually yields the total correction t_0 which is the sum of the two corrections t_0' and t_0''.

We can replace the infinitely long filament with an infinitely long cylinder so long as we realize that this may result in the temperature rise θ not being measured from the initial temperature of the needle, the time not being measured from the in-

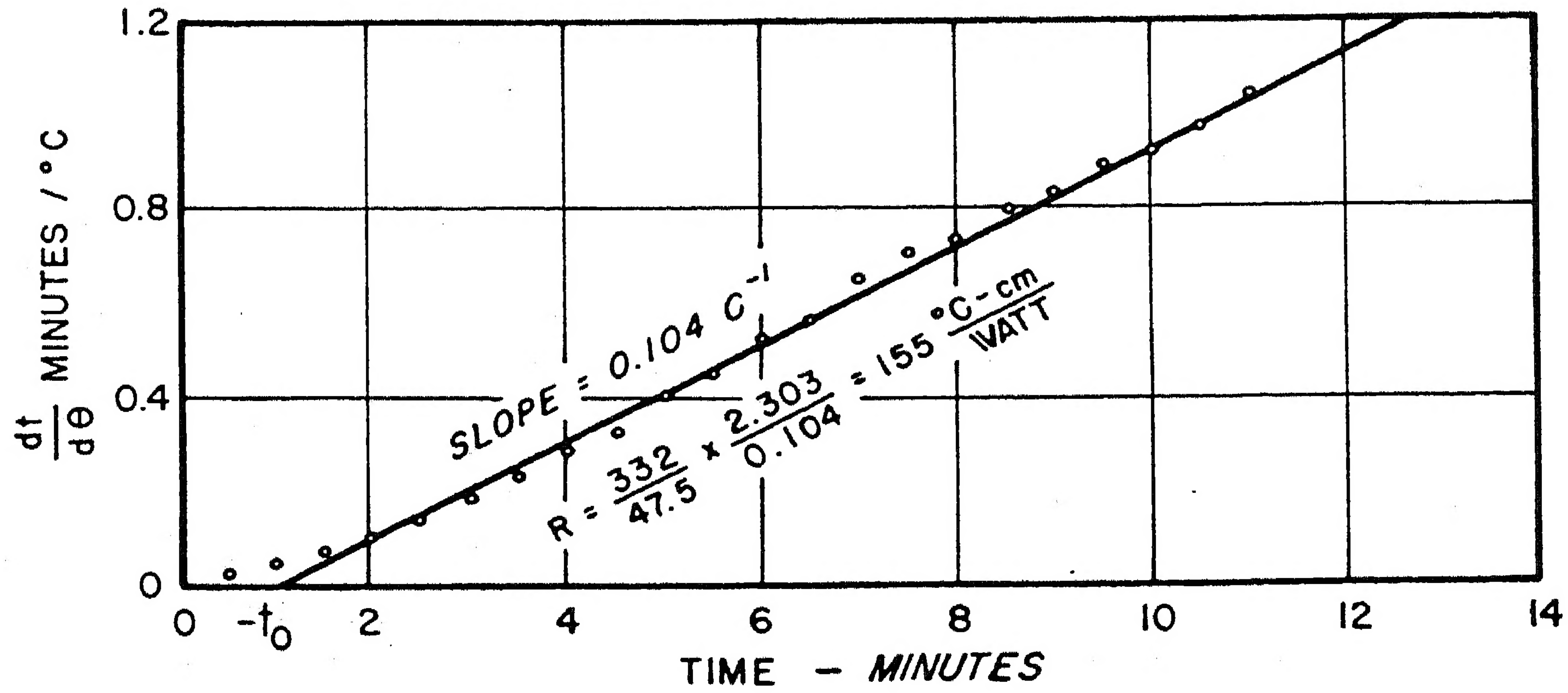

Figure 4. $\frac{dt}{d\theta}$ **versus t for the data of Figures 2 and 3**

*Total length is 5 to 8 feet, but only 2 to 5 feet is heated.

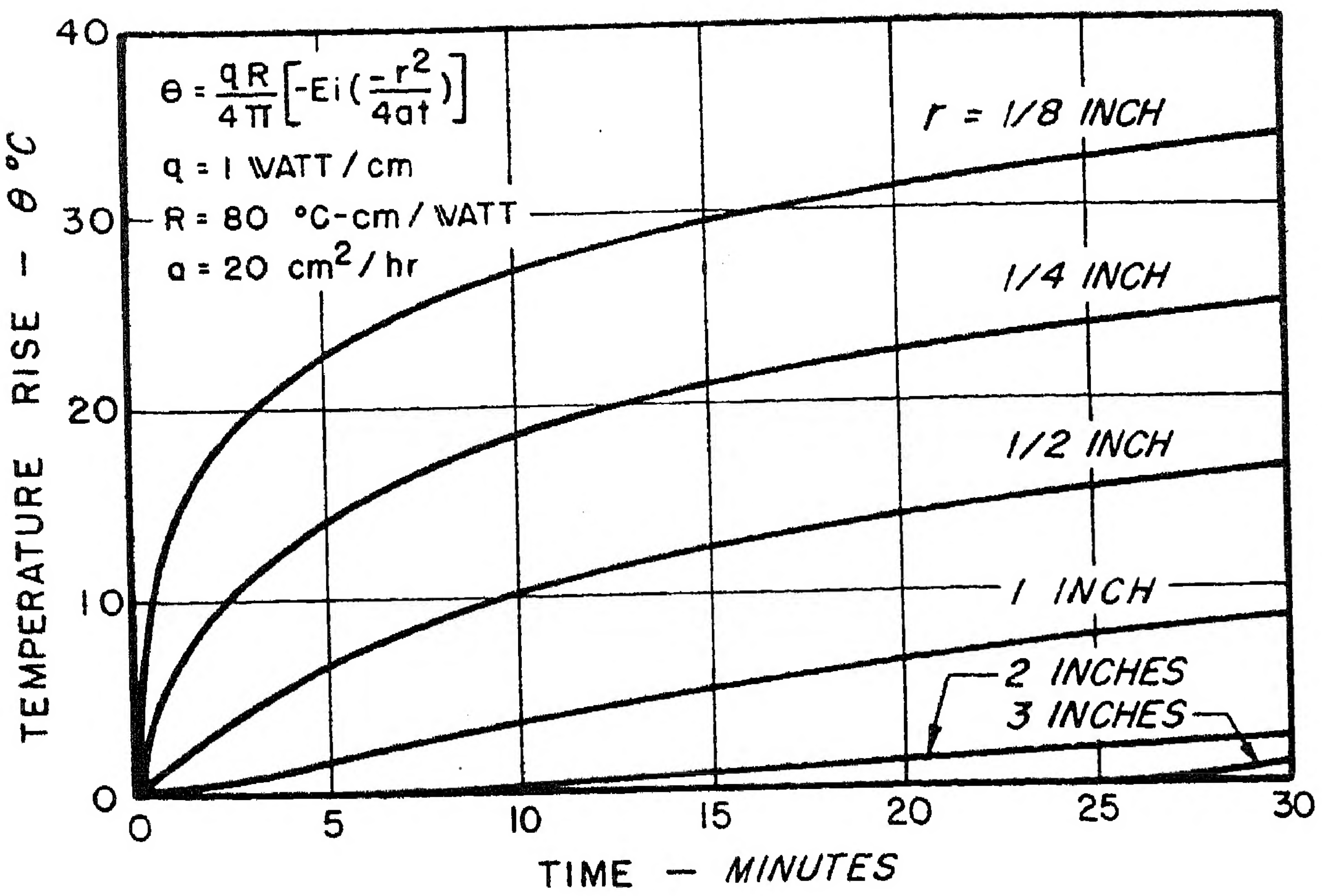

Figure 6 (above). Theoretical temperature rise curves for several values of r, and typical soil constants

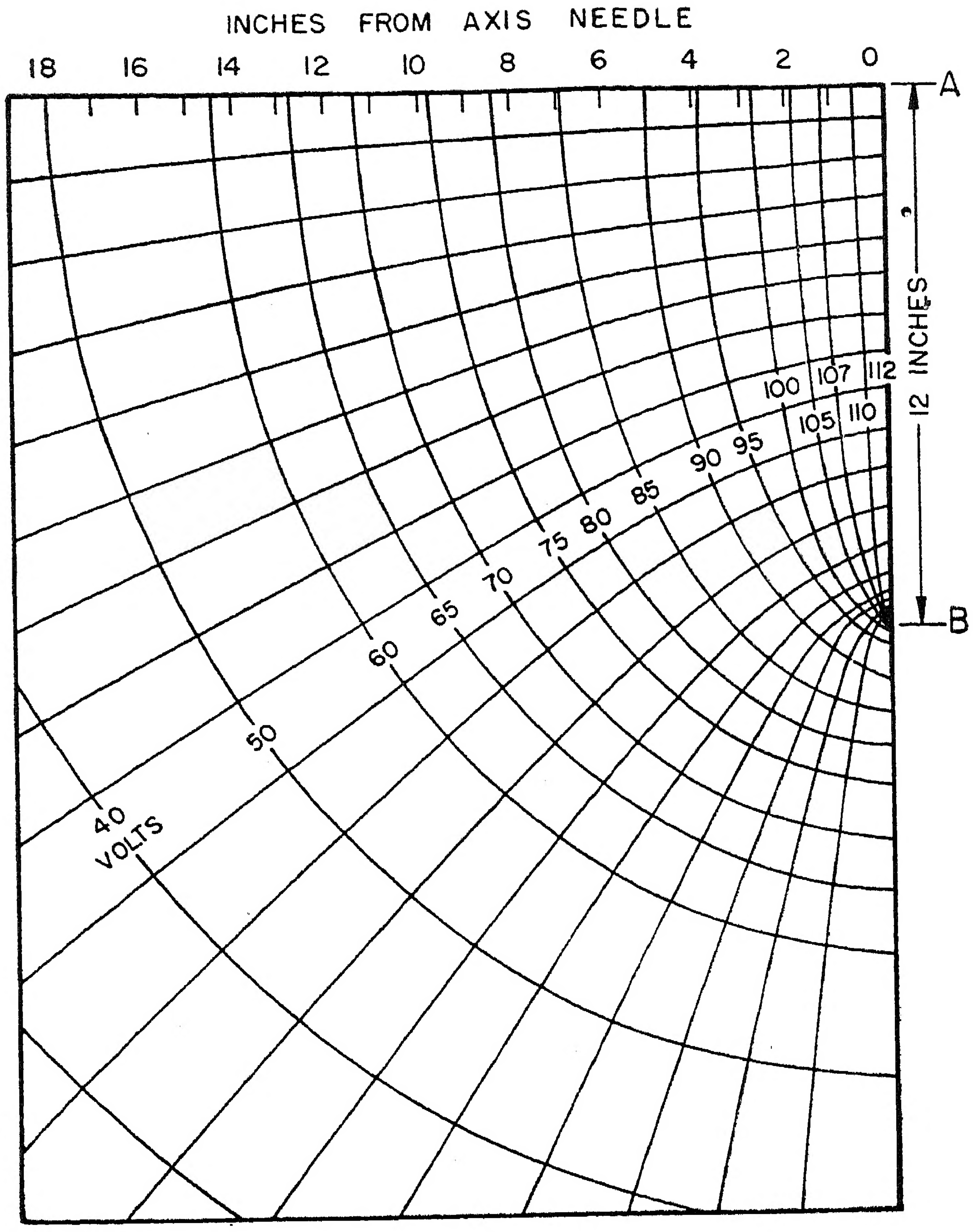

Figure 7 (right). Equipotential and flow lines for electrical model of needle

A. Mid-point of heater (thermocouple location)

B. End of needle (4 inches from A in model, corresponding to 12 inches on needle)

Voltage values indicate potential with respect to infinity (actually 90-degree arc, radius 14 inches, center A)

stant of switching on, and possibly, the distance r not being measured from the axis of the cylinder. Fortunately, the distance r does not enter into the above equation, and it is apparent that the thermal resistivity R is proportional to $\theta/\log t$, so that only the rate of change of θ with respect to $\log t$ is required. Also, for many soils it is accurate enough to assume that t is measured from the instant of switching on the power. The initial value of θ is avoided by using the change in θ in an interval starting some minutes after the heating power is switched on.

Effect of Finite Radius of Needle

There are two effects to be evaluated so far as the finite radius is concerned. These are:

1. The effect on the temperature rise at any point in the infinite medium at any time after switching on.
2. The effect of measuring the temperature rise of the needle rather than of the infinite medium.

The first of these effects has been evaluated by Van der Held[3] for a needle of uniform homogeneous material and shown more or less intuitively to be equivalent to a heat production prior to $t=0$. That is, it may be included as an addition to t_0'. To avoid confusion, let this change in zero time be accounted for by a time t_0'' to be added to $t+t_0'$ to give the total corrected time, $t'=t+t_0=t+t_0'+t_0''$.

The second effect seems to have escaped specific mention in the literature. Because of these apparent gaps in the reasoning, it was thought best to approach the measurements empirically by comparing the result of a transient needle determination with that obtained by the steady-state sphere method. This approach having proved successful, it is now possible to formulate a more theoretical basis for these effects.

Intuitively, it is reasonable that on a sufficiently gross scale, a 1/4-inch diameter rod will approximate an infinitely thin needle. Or, on a more nearly quantitative basis, when the rate of heat flow through the surface of the needle is very nearly equal to that being dissipated within it, that is, after the initial heating transient of the needle itself is practically over, the difference in its rate of temperature rise from that which would occur at the same radius for a filamentary source must be very small. After this time the rate of rise of temperature of the needle must for all practical purposes be equal to that of the infinite medium at the same radius and due to an ideal filamentary source. Thus the only difference here between the ideal and the practical cases is that the practical needle will appear to have been turned on before or after the ideal one depending upon whether the effective diffusivity of the needle is greater or less than that of the soil.

To evaluate the effect of measuring the temperature of the needle rather than of a point in the medium, consider that even with a homogeneous needle there is bound to be some thermal resistance at the interface between the needle and the medium. For a soil needle, made and used as described herein, there is the additional thermal resistance between the heater and the shell and the inevitably imperfect contact between the needle and the earth. Thus even when very little of the heater output is being used to raise the temperature of the needle, there still will be an almost constant temperature drop from the inside of the needle where the thermocouple is located to the soil just outside the shell. Thus the measured rise over the initial or ambient temperature will differ from the rise in the ideal theoretical case by an approximately constant amount after the initial needle heating transient is over.

These two effects tend to give a curve such as B, Figure 5, rather than A which is drawn for the ideal case. Curve B is for a needle with an effective diffusivity and resistivity greater than those of the soil. It is apparent from this discussion that the parts cd and ef of the two curves are practically identical in shape but are displaced both horizontally (in time) and vertically (in temperature). The time correction t_0'' may be taken care of in the

same way as was t_0', the correction to improve the fit of the approximate equation (equation 1) for θ. That this procedure is effective is proved in Appendix II. The temperature difference in the two curves A and B may be taken care of most easily by dealing only with rates of temperature rise or with temperature differences over time intervals starting after the initial needle-heating transient is over. In practice, this time may be recognized by the fact that the θ versus log t plot is curved for times less than this and straight thereafter.

This approach is intuitive and should be replaced when possible by a complete theory which takes into account the finite diameter of the needle and its thermal characteristics in more detail. Similar remarks apply with even more force to the following discussion of the effects of the finite length of the needle.

Effects of Finite Length

One of the effects of the finite length of the needle is that the heat loss from the ends of the needle—and from the unheated parts near the end—will cause a small temperature gradient along the needle. This will lead to an inaccuracy due to an incorrect value being taken for the net watts per centimeter entering the soil opposite the thermocouple. Other authors[3] have considered this and shown it to be small.

A second effect of the finite length is the spreading out of the heat-flow lines as they get farther and farther away from the needle. In a plane normal to the heater, the flow lines diverge because they are radial; but in a plane containing the heater axis, the heat-flow lines for an infinite needle are parallel and do not diverge. With the finite heater, the lines from the central part of the heater must bend outward to fill up the otherwise empty semicircles at the ends. Hence the heat leaving one centimeter of the heater will have to spread out over a greater and greater distance in this plane as it gets farther from the heater. Therefore, the temperature gradient along these flow lines will decrease with distance more rapidly for the finite needle than for an infinite one. The result is that the rate of temperature rise of a finite needle will decrease more rapidly with time than it would if the needle were infinite in length. If continued for long enough times, all practical θ versus log t curves will eventually bend gradually downwards from the straight part of the curve indicating that the end effect is becoming apparent. In some cases this makes the drawing of the best straight line very difficult.

In an effort to assess the magnitude of this effect theoretically, the family of curves shown in Figure 6 was prepared. These curves show the calculated temperature rise versus time relation at various distances from a transient needle for typical soil and power input conditions. To get some indication of the spreading of the heat-flow lines with distance from the needle, a very approximate steady-state, electrical model to a one-third scale was set up using a sheet of Teledeltos paper bounded by a 90-degree arc of metal with a radius of approximately 14 inches (equivalent to 42 inches) to represent a quarter-infinite medium and a small strip of brass approximately 4 inches long to represent the bottom 12 inches of a needle with a 24-inch heater. The resulting plot of equipotential lines which represent isothermals and the approximate heat-flow lines are shown in Figure 7.

From Figure 6 it is apparent that nothing happening at much more than 2 inches from the needle ($r=1/8$ inch) will have any noticeable effect on the needle for at least the first 15 minutes. From Figure 7 it is apparent that there is negligible divergence near the mid point of the needle for the first 2 or 3 inches. Therefore, if the shape of the steady-state equipotential lines of Figure 7 are reasonably close to the shape of the transient isothermals of the needle, the end effects should certainly not become important for the first 15 minutes and probably will not become too serious for the first 30 minutes after turning on the power. However, it must be remembered that the ratio of the electrical resistivities of the needle and soil models is very much greater than the ratio of the thermal resistivities of the actual needle and soil, and this has the effect of crowding the electric flow lines to the end of the needle more than the heat flow lines would be expected to crowd. This will cause less divergence in the region of interest in the electrical model than in the actual thermal case. It is also to be noted that a uniformly conducting sheet is not an exact model of a sector of a uniformly conducting solid medium.

Finite Nonhomogeneous Medium

If the heated portion of the needle is embedded deeply enough, the effects of the earth's surface will not affect the results until some time after the end effects previously described have become excessive.

Any inhomogeneity of the medium will cause the heat-flow lines to bend away from the paths they would follow in a homogeneous medium. Depending upon the shape and nature of the irregularities, they may cause unexpected kinks in the θ versus log t curve or may cause a general bending of the otherwise straight line. This may make it more difficult to decide upon the best straight line to draw, but with care it is usually possible to ob-

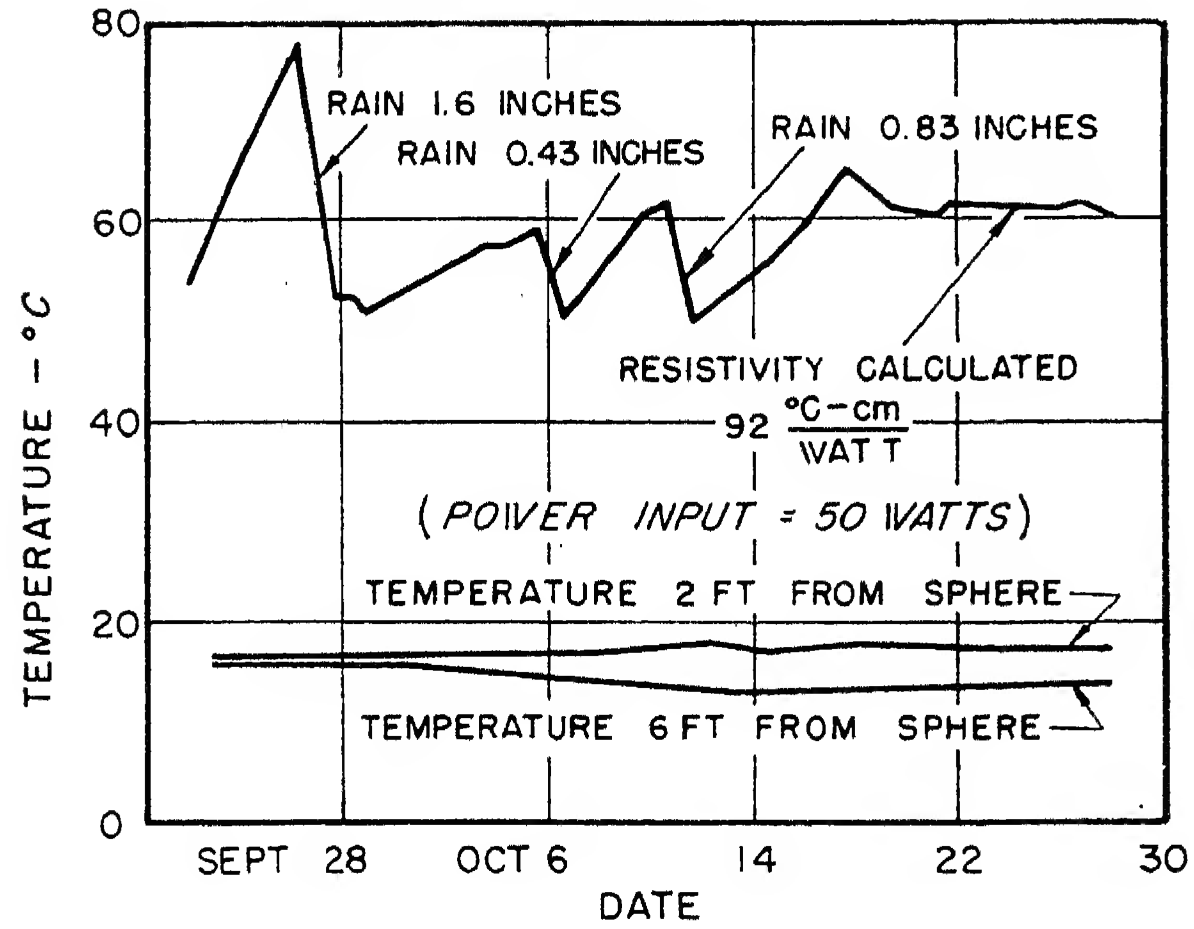

Figure 8. Steady-state sphere thermal resistivity test

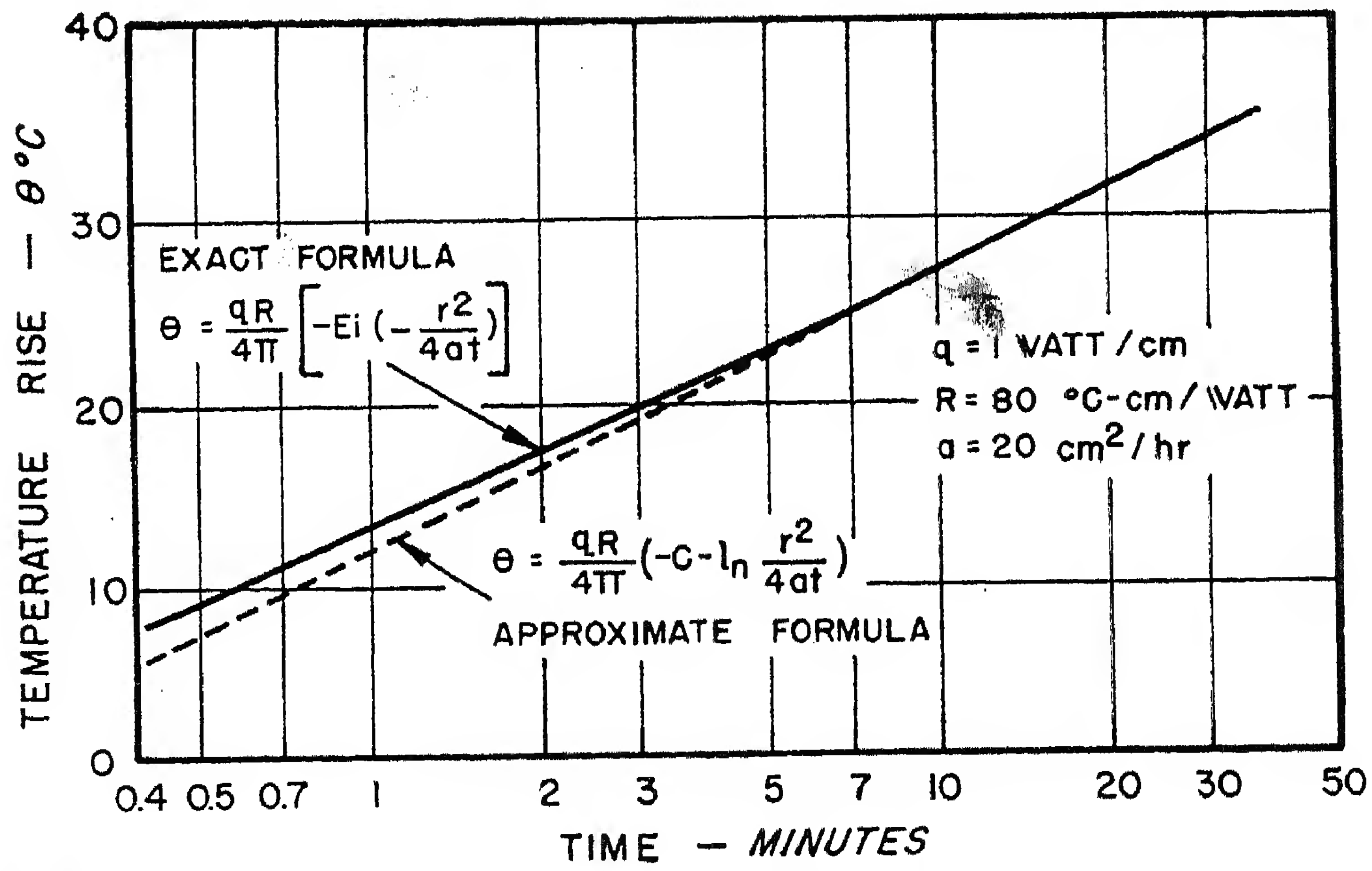

Figure 9. Theoretical temperature rise 1/8 inch from infinite filamentary source

tain a good average value for any soil condition.

The size of the sample measured by this method is not accurately definable because the material adjacent to the needle will have more effect on the first part of the curve than that farther away. The data listed previously from Figure 6 and 7, however, indicate that the average size of the sample measured is of the order of 3 inches diameter and 6 inches long.

Results

Figure 8 shows the variation of thermal resistivity with time for one installation of a steady-state sphere. At this particular location, the thermal resistivity varied very considerably with time due apparently to the initial recompaction of the soil and to variations in moisture content. Other installations, however, appeared to have resistivities almost completely independent of rainfall. One of the latter locations was used to compare the transient with the steady-state method.

With the transient needle method there will be no moisture migration effects and, therefore, an allowance should be made for this when making cable temperature calculations if its effect is likely to be significant.

A number of the transient θ-log t heating curves obtained appear to curve down from a straight line after 12 to 15 minutes. This may be due to the end effect becoming significant. While this is not in agreement with the data deduced from Figures 6 and 7, the approximations in the latter figure are so gross that it would be more surprising if there were good numerical agreement.

To date, over 100 soil thermal resistivity determinations have been made with the transient needle equipment. These include ones made on the earth surrounding a heat-pump ground coil for which purpose the multipoint needle has given results of particular interest.

Efforts are being made to achieve a much more complete theory by considering explicitly the finite size of the needle, the nonzero thermal resistivity at the needle surface, and the actual location of the thermocouples within the needle. It is hoped it will be possible soon to report the results of this theoretical work and the attendant experiments.

Conclusions

The transient needle method of measuring the thermal resistivity of soil is a convenient, rapid method which is capable of sufficient accuracy for the purpose of cable temperature calculations. While there are some parts of the theory not yet complete, the agreement found in practice between this and the steady-state sphere method is sufficient to allow the new method to be used with confidence.

Appendix I. The Mathematical Theory of the Transient Needle

The temperature rise θ in degrees Celsius at a point a distance r centimeters from an infinite filamentary heat source releasing heat at the rate of q watts per centimeter is as shown in section 9.8 of reference 5

$$\theta=\frac{qR}{2\pi}\int_{\beta}^{\infty}\frac{\epsilon^{-\beta^2}}{\beta}d\beta=\frac{qR}{2\pi}I(\beta) \tag{2}$$

where

R = thermal resistivity of the medium in thermal ohm-centimeters

$\beta=r/2\sqrt{at}$

a = diffusivity of the medium in square centimeters per hour

t = time from start of release of heat in hours

This function $I(\beta)$ is tabulated in Appendix F of reference 5. Equation 2 may also be written in the form

$$\theta=\frac{qR}{4\pi}\int_{x}^{\infty}\frac{e^{-x}}{x}dx=\frac{qR}{4\pi}[-Ei(-x)] \tag{3}$$

where $x=\beta^2=r^2/(4at)$

The function $-Ei(-x)$ is very fully tabulated in reference 6. For small values of x, these integrals may be evaluated in the form of a series, giving

$$\theta=\frac{qR}{4\pi}\left[-C-ln\ x+\frac{x}{(1!1)}-\frac{x^2}{(2!2)}+\frac{x^3}{(3!3)}-\ldots\right] \tag{4}$$

where C = Euler's constant = 0.5772

If x is small (that is, for small distances r and long times t), only the first two terms of the preceding series are significant and therefore

$$\theta=\frac{qR}{4\pi}\left[ln\ t-ln\frac{r^2}{4a}-C\right] \tag{5}$$

The accuracy with which this approximation holds for a typical soil is illustrated by Figure 9 which shows the actual tabulated values of $-Ei(-x)$ plotted on semilog paper. It is seen that these curves are very nearly straight lines for t greater than 3 minutes.

The improved method of approximation given by Van der Held[3] is obtained by substituting for the value of x in equation 3 and obtaining

$$\theta=\frac{qR}{4\pi}\int_0^t\frac{\epsilon^{-r^2/4at}}{t}dt \tag{6}$$

Whence, by differentiating both sides with respect to time t, and inverting

$$\frac{dt}{d\theta}=\frac{4\pi}{qR}te^{x}$$

$$=\frac{4\pi}{qR}t\left[1+x+\frac{x^2}{2!}+\frac{x^3}{3!}+\ldots\right]$$

$$=\frac{4\pi}{qR}\left[t+\frac{r^2}{4a}+\frac{t}{2!}\left(\frac{r^2}{4at}\right)^2+\ldots\right] \tag{7}$$

Again in this series, the third term is very small for small r and large t and so may be neglected. Putting $r^2/4a=t_0$,* we get

$$\frac{dt}{d\theta}=\frac{4\pi}{qR}(t+t_0)$$

The value of t_0 can, therefore, be obtained by plotting $dt/d\theta$ against t and finding the t-axis intercept of the linear portion. This intercept is to the left of the origin for positive t_0 because for $dt/d\theta=0$, $t=-t_0$.

Appendix II. Proof of Construction for t_0''

Following Figure 5 and the accompanying discussion, consider that the equation of curve A is given by equation 3, while that of curve B is

$$\theta=\frac{qR}{4\pi}\int_0^{t'}\frac{\epsilon^{-r^2/4at'}}{t'}dt'+\theta'(t) \tag{8}$$

where $t'=t+t_0''$, and $\theta'(t)$ is the temperature drop from the thermocouple to the surface of the soil around the needle due to the internal thermal resistance in the needle and the resistance at the needle-soil interface. After a very few minutes, θ' will be constant.

Differentiating equation 8 with respect to the time from turn on t, and since t_0' = constant, we have for times t large enough to make $\theta'(t)$ a constant

$$\frac{d\theta}{dt}=\frac{qR}{4\pi}\frac{\epsilon^{-r^2/4at'}}{t'} \tag{9}$$

Hence, as before

$$\frac{dt}{d\theta}=\frac{4\pi}{qR}t'\epsilon^{r^2/4at'}$$

$$=\frac{4\pi}{qR}t'\left[1+r^2/4at'+\frac{(r^2/4at')^2}{2!}+\ldots\right]$$

$$=\frac{4\pi}{qR}(t'+r^2/4a)\quad\text{neglecting small terms}$$

$$=\frac{4\pi}{qR}(t+t_0'+t_0'') \tag{10}$$

Hence, plotting $dt/d\theta$ against t gives the x-intercept of value $-t_0=-(t_0'+t_0'')$. Since t_0'' is negative for cases where the thermal diffusivity of the needle is less than that of the soil and since in most cases t_0'' is greater than t_0' the x-intercept can lie to the right of the origin.

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*Others[3,4] have used $r^2/4a=-t_0$, but this appears to be only a needless complication of signs since r^2 and a are both positive.

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No Discussion

Accurate Computation of 2-Machine Stability

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THE usual assumption that the constant voltage busses of a 2-machine stability problem are connected by pure reactances results in a number of approximations which may or may not be valid. On the other hand, if complicating factors such as losses, intermediate loads, transformer taps, and so forth are included, the process of obtaining a solution may become very laborious indeed.

The method of solution described in this paper preserves accuracy by permitting the consideration of all system parameters—including the inertia of both machines—without approximation. Yet the procedure is relatively simple, even when line reclosure is to be considered in the switching schedule. Part of the simplification is due to the utilization of generalized swing curves to determine relative angle and velocity at any time during the swing. Similar curves have been described previously,[1] but the present method of derivation removes the former restrictions as to type of system disturbance for which they are applicable.

This method is well adapted to the accurate determination of transient power limits because the computations involve sets of composite general circuit constants,[2] which need not change as the power transfer in the system is varied. Consequently, they and many auxiliary quantities need be computed only once. An auxiliary curve provides a quantitative measure of the degree of stability or instability of the system.

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General Theory

To utilize the swing equation[1,3,4]

$$d^2\delta/dt^2 = K'(P_0 - P) \tag{1}$$

where

δ = phase angle between the two machines

$$K' = \frac{(\pi f)\ \text{system kilovolt-ampere base}}{(H)\ \text{machine kilovolt-ampere base}} \tag{2}$$

P_0 = steady-state or initial machine power, in per unit

P = machine power at time t

(note that these quantities are normally positive for both receiving- and sending-end machines)

it is necessary to find a relation between δ and P. In section A of Appendix I it is shown that

$$P_S = P_M \sin(\delta - \phi) + P_S' \tag{3}$$

$$P_R = P_M \sin(\delta + \phi) - P_R' \tag{4}$$

in which

the subscripts S and R indicate sending- and receiving-end quantities respectively

$$P_M = e_S e_R / b \tag{5}$$

$$\phi = 90 - \beta \tag{6}$$

$$P_S' = (e_S/b)^2 (\boldsymbol{DB})_{RP} \tag{7}$$

$$P_R' = (e_R/b)^2 (\boldsymbol{AB})_{RP} \tag{8}$$

e_S = absolute value of sending-machine voltage, in per unit

e_R = absolute value of receiving-machine voltage, in per unit

$A = A_r + jA_i$

$B = B_r + jB_i = b\underline{|\beta}$

$D = D_r + jD_i$

$\boldsymbol{AB} = (\boldsymbol{AB})_{RP} + j(\boldsymbol{AB})_{IP} = (A_r - jA_i)(B_r + jB_i)$

$\boldsymbol{DB} = (\boldsymbol{DB})_{RP} + j(\boldsymbol{DB})_{IP} = (D_r - jD_i)(B_r + jB_i)$

Hence it is possible to write two swing equations, one for the sending-end machine and the other for the receiving-end machine. These are added to give a complete description of the angle δ. After integrating once and rearranging terms, see section B of Appendix I, the result can be expressed in the concise form

$$\omega = d\theta/dt = d\delta/dt = \sqrt{2K'P_M}\sqrt{R\theta + \cos\theta - C} \tag{9}$$

in which

ω is the relative machine velocity

$$K' = \sqrt{(K_S')^2 + (K_R')^2 + 2K_S'K_R' \cos 2\phi} \tag{10}$$

$$R = (K_S'P_{S0}' + K_R'P_{R0}')/K'P_M \tag{11}$$

$$\theta = \delta - \psi \tag{12}$$

$$\tan\psi = [(K_S' - K_R')/(K_S' + K_R')] \times \tan\phi \tag{13}$$

$$P_{S0}' = P_{S0} - P_S' \tag{14}$$

$$P_{R0}' = P_{R0} + P_R' \tag{15}$$

C is a constant of integration, determined from initial boundary conditions

It is convenient to let

$$W = \sqrt{2K'P_M} \tag{16}$$

$$\omega' = \sqrt{R\theta + \cos\theta - C} \tag{17}$$

so that equation 9 becomes

$$\omega = W \times \omega' \tag{18}$$

If the system is stable, then there must be a value of θ greater than the equilibrium value θ_e, for which ω is zero. It is evident that solving the equation

$$R\theta + \cos\theta - C = 0 \tag{19}$$

will yield the desired value of θ if it exists. If it does not exist, the system is unstable.

Another deduction from equations 9 and 17 is that the maximum and minimum values of ω and ω' occur when

$$R = \sin\theta \tag{20}$$

The maximum corresponds to θ_e in the first quadrant; the minimum corresponds to θ_M in the second quadrant. But if ω has a minimum value, it does not become zero, and the system must be unstable. A borderline case occurs when ω' and its

derivative with respect to θ are zero simultaneously. Hence a simultaneous solution of equations 19 and 20 yields the borderline equation

$$R \arcsin R + \sqrt{1-R^2} - C = 0 \qquad (21)$$

This equation is plotted as a curve in Figure 1. On the left of the curve the system is stable; on the right, unstable. When computed values of R and C for a system are plotted on Figure 1, the distances of the plotted points from the curve are quantitative measures of the degree of stability or instability of the system.

Generalized Time—Angle Curves

In the preceding equations it has been assumed that C can be found from the boundary values between two conditions of switching. If the previous condition was a 3-phase fault through which no synchronizing power could be transmitted, the boundary values of ω and δ can be found easily from the equations

$$\omega_{S1} = K_S'(P_{S0} - P_S)t + \omega_{S0} \quad \text{(radians per second)} \qquad (22)$$

$$\delta_{S1} = 57.3[{}^1/_2 K_S'(P_{S0} - P_S)t^2 + \omega_{S0}t] + \delta_0 \quad \text{(degrees)} \qquad (23)$$

which are obtained directly from equation 1.

Similar equations apply to the receiving-end machine; and the results can be added directly. Note that the inclusion of the ω_0 term accommodates reclosing on a 3-phase fault. For an initial fault, ω_0 is zero.

For other previous switching conditions, such as an unbalanced fault or a line out of service before reclosure, it may be possible to transmit synchronizing power through the system. The problem then is to find the angle δ(or θ) at some preassigned time, t, after the swing has started. The solution is to integrate equation 9

$$W dt = d\theta/\sqrt{R\theta + \cos\theta - C} \qquad (9)$$

$$W\int_{t_1}^{t_2} dt = \int_{\theta_1}^{\theta_2} d\theta/\sqrt{R\theta + \cos\theta - C} \qquad (24)$$

$$= \int_{\theta_e}^{\theta_2} d\theta/\sqrt{R\theta+\cos\theta - C} - \int_{\theta_e}^{\theta_1} d\theta/\sqrt{R\theta + \cos\theta - C}$$

or

$$W(t_2 - t_1) = T_2 - T_1 \qquad (25)$$

where

$$T = \int_{\theta_e} d\theta/\sqrt{R\theta + \cos\theta - C} \qquad (26)$$

The lower limit can be any arbitrary value of θ. It is convenient to let it equal θ_e because ω' will be a maximum at this point for any value of R, regardless of the value of C.

As yet no direct way of integrating equation 24 or equation 26 has been found. It can be done indirectly, however, by computing incremental areas utilizing Simpson's Rule for appropriate values of R and C. The results are similar to the sets of curves of T versus θ shown in Figures 2 and 3. Since ω' had to be computed for each point, curves of ω' versus θ can be plotted also on the same sheet. It will be noted that the curves are similar to conventional swing curves with the axes interchanged. This variation was desirable to utilize the same θ axis for both sets of curves—ω' and T.

At the values $\theta = \theta_{x1}$ and $\theta = \theta_{x2}$ for which $\omega' = 0$ the integrand of equation 26 increases without limit. It can be shown that the integral converges for these values, however, by expanding $\cos\theta$ as a Taylor's series about θ_{x1} or θ_{x2}, and dealing only with incremental values of θ. The borderline value, θ_M, is a special case for which the integral increases without limit.

The Taylor's series expansion provides an equation from which the area of the integral can be computed for the last increment of θ adjacent to θ_{x1} or θ_{x2} (derived in section C of Appendix I).

$$\Delta T_x = 0.264\sqrt{\Delta\theta/(R - \sin\theta_x)} \qquad (27)$$

where $\Delta\theta$ is usually taken to be 0.1 degree.

Solution of a Problem

Consider the system of Figure 4 in which a 3-phase fault is to be applied to one of the 115-kv lines near station A. The fault is to be cleared in 6 cycles and the line reclosed successfully in 21 cycles. Station A is to deliver 50 megawatts at unity power factor to the 13.8-kv bus at station B under steady-state conditions. The outline of the solution is given here, and the actual computations are carried out in Appendix II.

1. Set up a steady-state power flow.
2. Find the voltage behind transient reactance and the initial power output of each machine; also the phase angle δ_e between the machine voltages.
3. Compute composite constants for the system between these points for (a) both lines in service; (b) only one line in service.
4. Compute the constants from behind transient reactance of each machine to the station A 115-kv bus (point of fault). Both lines are in service.
5. Tabulate values of K_S', K_R', e_S, e_R, P_{S0}, P_{R0}, P_M, ϕ, P_S', P_R', K', R, ψ, P_{S0}', P_{R0}' and W for each set of constants.
6. Apply the fault and find the relative velocity and angle 6 cycles, or 0.10 second later.
7. For the fault-cleared condition, find the boundary values ω_1' and θ_1.
8. From the curves of θ versus ω' find C.
9. For these values of θ and C find T_1.
10. From equation 25 compute T_2, where t_2 is 21 cycles.
11. From the T curves find θ_2.
12. From the ω' curves find ω_2'.
13. Find δ_2 and ω_2.
14. With line reclosed, recompute θ_2 and ω_2'.
15. Compute C.
16. From Figure 1 determine if the system is stable.

Conclusions

Curves similar to Figures 2 and 3 have been plotted for values of R ranging from 0.3 to 4.0. For values of R below 0.7, the system is almost certain to be stable; for R greater than 1.0, it is certainly unstable. Conditions subsequent to clearing the fault usually fall in the range of R between 0.8 and 1.1. In this range fast reclosure of the faulted circuit normally will be effective in maintaining stability.

Curves for R greater than about 1.5 represent fault conditions for which some synchronizing power can be transmitted. Synchronism will be lost, however, if the

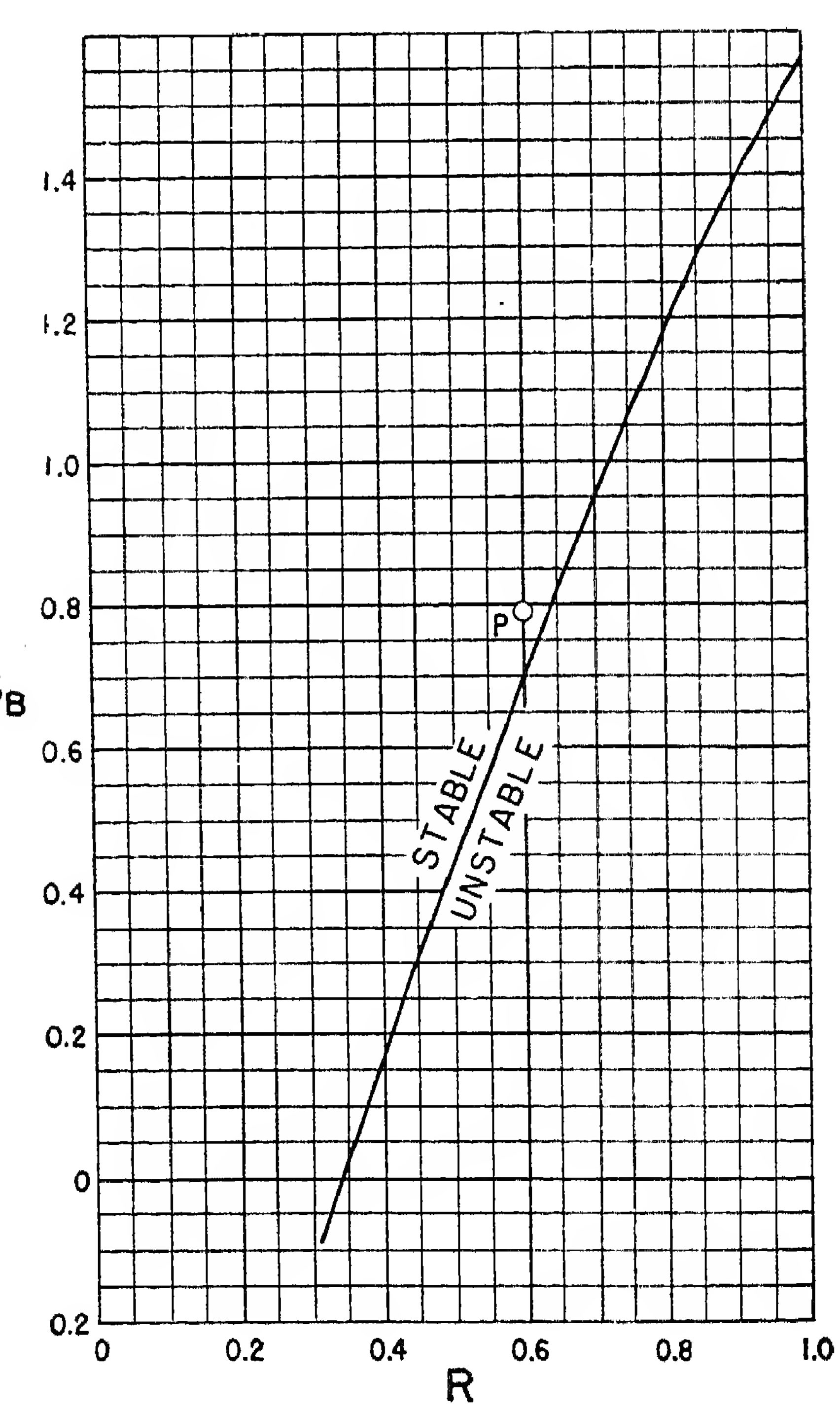

Figure 1. Curve of R versus C for determining degree of system stability

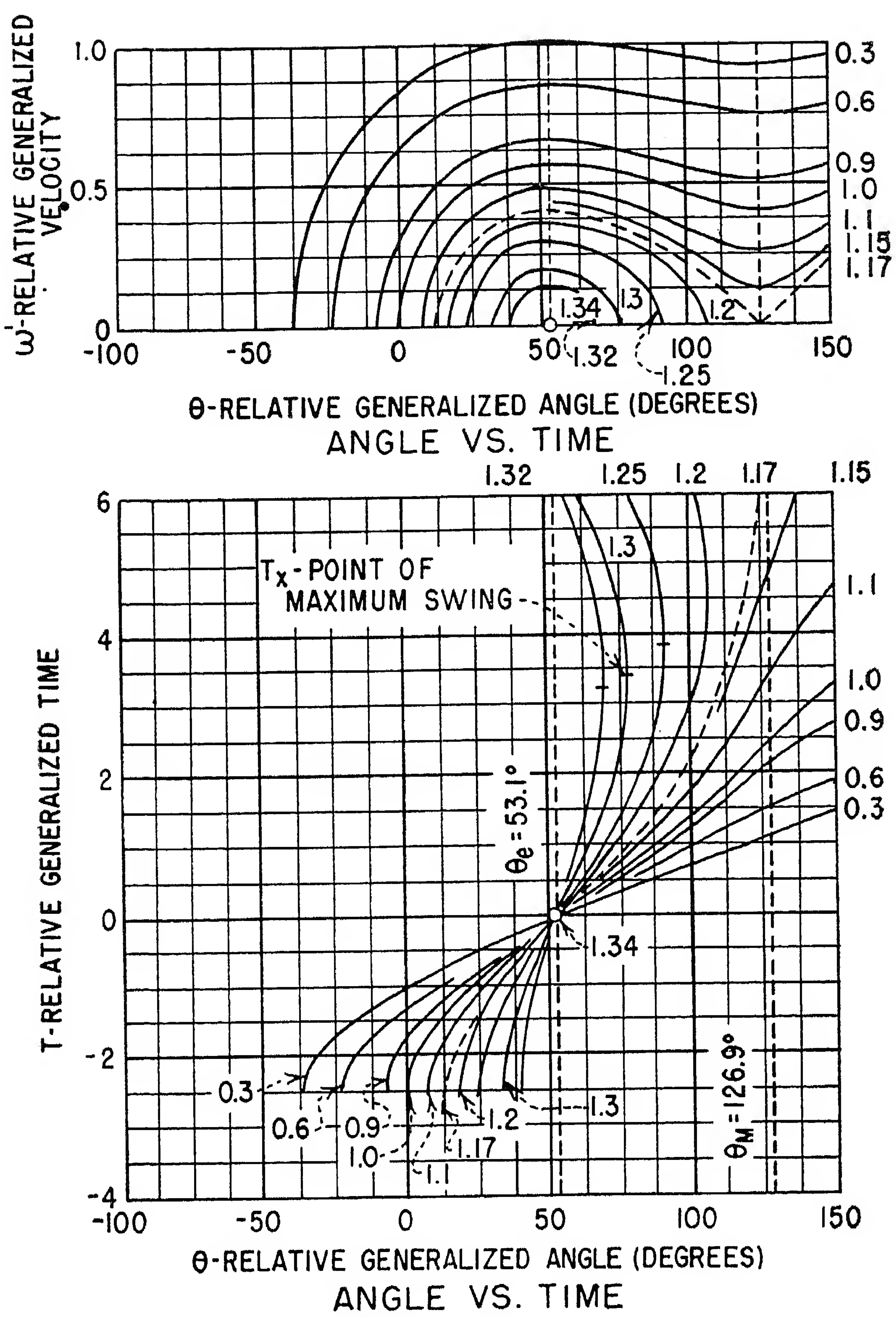

Figure 2. Generalized stability curves for R = 0.80

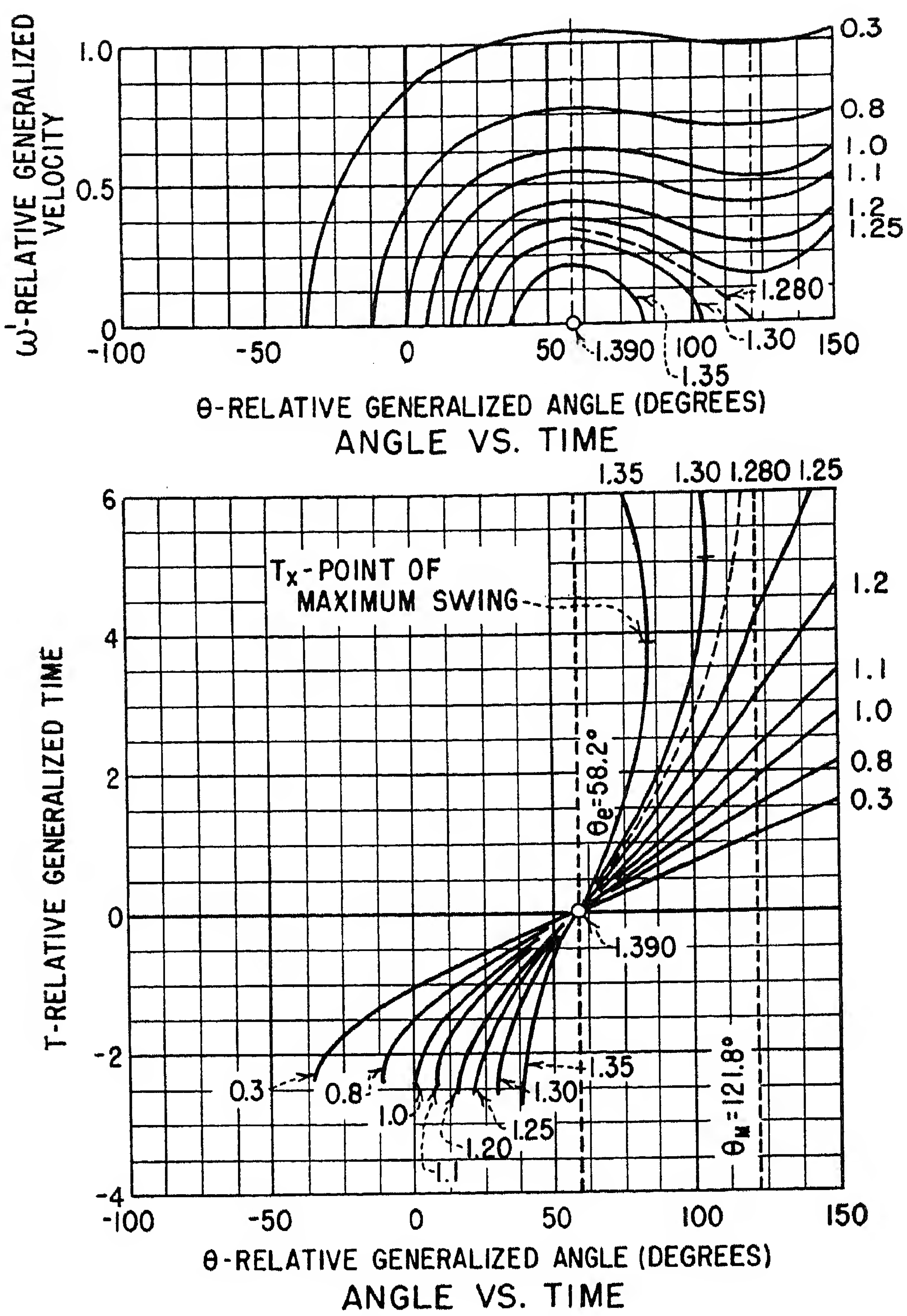

Figure 3. Generalized stability curves for R = 0.85

condition persists.

If greater accuracy is desired than that obtainable from preplotted curves, equations 24 and 27 can be utilized to perform a numerical integration for any values of R and C. The principal sources of error are the incorporation of constant power loads into the composite general circuit constants as constant impedances and the incremental integration of equation 24. The former approximation is regularly accepted in network-analyzer studies, and the latter can be minimized by using small increments.

Appendix I

A. Derivation of Equations 3 and 4

These equations are based on the universal circle diagram[5] equations

$$M_S^2+N_S^2=P_M^2 \tag{28}$$

$$M_R^2+N_R^2=P_M^2 \tag{29}$$

$$N_S/M_S=\tan(\beta+\delta) \tag{30}$$

$$N_R/M_R=\tan(\beta-\delta) \tag{31}$$

$$M_S=P_S-P_S' \tag{32}$$

$$M_R=P_R+P_R' \tag{33}$$

It can be shown that sending-end operation lies in the third and fourth quadrants, and receiving-end operation lies in the first quadrant.

Hence

$$M_S=-P_M\cos(\beta+\delta) \tag{34}$$

$$M_R=P_M\cos(\beta-\delta) \tag{35}$$

or

$$\begin{aligned} P_S-P_S' &= -P_M\sin[90-(\beta+\delta)] \\ &= -P_M\sin(\phi-\delta) \\ P_S &= P_M\sin(\delta-\phi)+P_S' \end{aligned} \tag{3}$$

$$\begin{aligned} P_R+P_R' &= P_M\sin[90-(\beta-\delta)] \\ &= P_M\sin(\phi+\delta) \\ P_R &= P_M\sin(\delta+\phi)-P_R' \end{aligned} \tag{4}$$

B. Derivation of Equation 9

Let

δ_e = steady-state equilibrium angle between sending- and receiving-end machines

δ_S = deviation of sending-end machine from equilibrium angle

δ_R = deviation of receiving-end machine from equilibrium angle

$$\delta=\delta_e+\delta_S+\delta_R \tag{36}$$

The sending- and receiving-end swing equations are

$$d^2\delta_S/dt^2=K_S'(P_{S0}-P_S) \tag{37}$$

$$d^2\delta_R/dt^2=K_R'(P_{R0}-P_R) \tag{38}$$

Substitute equations 3, 4, and 36 and add

$$d^2\delta/dt^2=K_S'[P_{S0}-P_M\sin(\delta-\phi)-P_S']+K_R'[P_{R0}-P_M\sin(\delta+\phi)+P_R'] \tag{39}$$

Substitute equations 14 and 15 and rearrange

$$d^2\delta/dt^2=[K_S'P_{S0}'+K_R'P_{R0}']-P_M[K_S'\sin(\delta-\phi)+K_R'\sin(\delta+\phi)] \tag{40}$$

Multiply by the integrating factor

$$2(d\delta/dt)dt=2d\delta \tag{41}$$

and integrate

$$(d\delta/dt)^2=2[K_S'P_{S0}'+K_R'P_{R0}']\delta+2P_M[K_S'\cos(\delta-\phi)+K_R'\cos(\delta+\phi)]-C_2 \tag{42}$$

Expand the second term on the right-hand side

$$\begin{aligned} K_S'\cos(\delta-\phi)+K_R'\cos(\delta+\phi) &= K_S'\cos\phi\cos\delta+K_S'\sin\phi\sin\delta+K_R'\cos\phi\cos\delta-K_R'\sin\phi\sin\delta \\ &= (K_S'+K_R')\cos\phi\cos\delta+(K_S'-K_R')\sin\phi\sin\delta \end{aligned} \tag{43}$$

Let

$$(K_S'+K_R')\cos\phi=K'\cos\psi \tag{44}$$

$$(K_S'-K_R')\sin\phi=K'\sin\psi \tag{45}$$

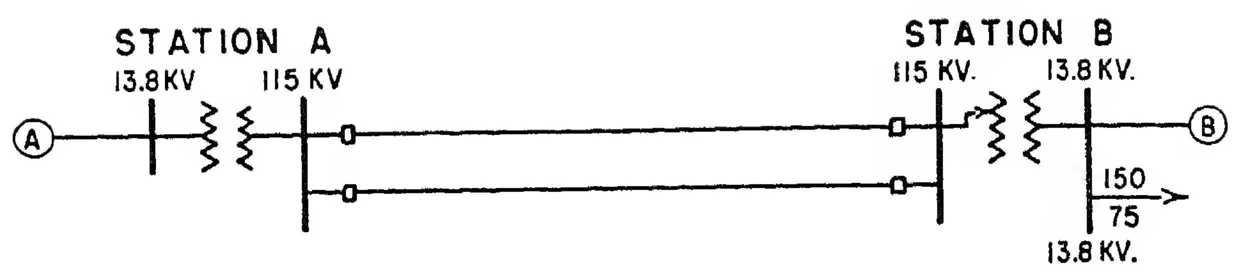

Figure 4. Hypothetical system for determination of system stability

System data:

Generators: A—60 megavolt-amperes, $X_d'=33$ per cent, $H=2.5$; B—120 megavolt-amperes, $X_d'=20$ per cent, $H=5.0$

Transformers: A—60 megavolt-amperes, 13.8–115 kv, $Z=0.005+j0.100$, $Y=0.003-j0.030$, +5-per-cent high-voltage tap; B—60 megavolt-amperes, 110–13.8 kv, $Z=0.005+j0.010$, $Y=0.003-j0.030$, $\pm 2^1/_2$ per cent, ± 5 per-cent high-voltage tap

Lines: Two 115 kv, 150 miles, 397,500-circular-mil steel-reinforced aluminum cable, 12-foot flat spacing

To find equations for K' and ψ

Square equations 44 and 45 and add

$$(K')^2=(K_S'+K_R')^2\cos^2\phi+(K_S'-K_R')^2\sin^2\phi$$
$$=(K_S')^2+(K_R')^2+2K_S'K_R'(\cos^2\phi-\sin^2\phi) \quad (46)$$

$$K'=\sqrt{(K_S')^2+(K_R')^2+2K_S'K_R'\cos 2\phi} \quad (10)$$

Divide equation 45 by equation 44

$$\tan\psi=[(K_S'-K_R')/(K_S'+K_R')]\tan\phi \quad (13)$$

Substitute equations 44 and 45 in 43

$$K_S'\cos(\delta-\phi)+K_R'\cos(\delta+\phi)=K'\cos\psi\times\cos\delta+K'\sin\psi\sin\delta=K'\cos(\delta-\psi) \quad (47)$$

Substitute equation 47 in equation 42

$$(d\delta/dt)^2=2[K_S'P_{S0}'+K_R'P_{R0}']\delta+2P_MK'\cos(\delta-\psi)-C_2 \quad (48)$$

Let

$$R=(K_S'P_{S0}'+K_R'P_{R0}')/K'P_M \quad (11)$$

$$(d\delta/dt)^2=2K'P_M[R\delta+\cos(\delta-\psi)-C_1] \quad (49)$$

Let

$$\theta=\delta-\psi \quad (12)$$

$$(d\theta/dt)^2=2K'P_M(R\theta+\cos\theta-C) \quad (50)$$

$$d\theta/dt=\sqrt{2K'P_M}\sqrt{R\theta+\cos\theta-C} \quad (9)$$

C. Derivation of Equation 27

In equation 24

$$\Delta T=\int_{\theta_1}^{\theta_2} d\theta/\sqrt{R\theta+\cos\theta-C}$$

Let

$$\theta_1=\theta_x,\ \theta_2=\theta_x+\Delta\theta$$

where

$$R\theta_x+\cos\theta_x-C=0$$

Then

$$\Delta T_x=\int_{\theta_x}^{\theta_x+\Delta\theta}\frac{d(\theta_x+\Delta\theta)}{\sqrt{R(\theta_x+\Delta\theta)+\cos(\theta_x+\Delta\theta)-C}}$$
$$=\int_0^{\Delta\theta}\frac{d\Delta\theta}{\sqrt{R\theta_v+R\Delta\theta+\cos\theta_x\times\cos\Delta\theta-\sin\theta_x\sin\Delta\theta-C}}$$

If $\Delta\theta$ is small enough

$$\cos\Delta\theta=1$$
$$\sin\Delta\theta=\Delta\theta$$

Then

$$\Delta T_x=\int_0^{\Delta\theta}\frac{d\Delta\theta}{\sqrt{(R\theta_x+\cos\theta_x-C)+R\Delta\theta-\Delta\theta\sin\theta_x}}$$

$$\Delta T_x=\int_0^{\Delta\theta}d\Delta\theta/\sqrt{(R-\sin\theta_x)\Delta\theta}$$
$$=2\left[\sqrt{\Delta\theta}/\sqrt{R-\sin\theta_x}\right]_0^{\Delta\theta}$$
$$=2\sqrt{\Delta\theta/(R-\sin\theta_x)}$$

When $\Delta\theta$ is expressed in degrees

$$\Delta T_x=(2/\sqrt{57.3})\sqrt{\Delta\theta/(R-\sin\theta_x)}$$
$$=0.264\sqrt{\Delta\theta/(R-\sin\theta_x)} \quad (27)$$

For the upper value, θ_{x2}, $\Delta\theta$ is negative. But $\sin\theta_x$ is greater than R for this condition, so that the quantity under the radical is still positive. Consequently, equation 27 can be used for computing ΔT_{x2}.

Appendix II

The following numbered items correspond to the numbers used in the discussion of the problem. All quantities are in per unit on a 50-megavolt-ampere base.

1. The detailed computations of the steady-state power flow are not included here. It was found advisable to operate with the +5 per-cent fixed tap at station A and the −5 per-cent fixed tap at station B.

2. From 1.

$e_S=1.045$ $\quad e_R=1.136$
$P_{S1}=1.098$ $\quad P_{R0}=-2.000$
$\delta_e=32.3$ degrees

Note that P_{R0} is negative, because P_R is inherently positive for power flow out of the system.

3. For both lines in service

$A=1.953+j1.955$
$B=-0.089+j0.844=0.848\ \underline{|96.0 \text{ degrees}}$
$C=3.286-j1.230$
$D=1.185+j0.291$
$AB=1.476+j1.822$
$DB=0.140+j1.026$

For one line in service

$A=2.613+j2.545$
$B=-0.063+j1.133=1.134\ \underline{|93.2 \text{ degrees}}$
$C=3.360-j1.490$
$D=1.231+j0.294$
$AB=2.719+j3.121$
$DB=0.256+j1.728$

4. From machine A to point of fault

$A=0.962+j0.001$
$B=0.004+j0.377$
$C=0.004-j0.034$
$D=1.053+j0$
$D/B=0.030-j2.793$

From machine B to point of fault

$A=1.579+j0.825$
$B=0.011+j0.441$
$C=3.088-j1.120$
$D=1.111+j0.275$
$D/B=0.686-j2.502$

For these constants the load on the station B 13.8-kv bus has been assumed to be a constant admittance

$Y=3.000-j1.500$

The quantity D/B is the short-circuit admittance of the system; it is multiplied by e^2 to yield the power output of a machine with the fault applied.

5. In Table I the values of P_S' and P_R' for the fault-on condition are actually the values of P_S and P_R, which are constant because no power can be transmitted through the 3-phase fault.

6. Since no power can be transmitted through the fault, equations 22 and 23 can be used. Otherwise the procedure would be similar to that used for the "fault cleared" stage of the computations.

For the sending end

$$\omega_{S0}=0,\ \delta_{S0}=0$$
$$\omega_{S1}=K_S'(P_{S0}-P_S)t$$
$$=62.840(1.098-0.033)0.100$$
$$=6.692 \text{ radians per second} \quad (22)$$

$$\delta_{S1}=57.3[^1/_2K_S'(P_{S0}-P_S)t^2]$$
$$=57.3[^1/_2\times 62.840(1.098-0.033)0.01]$$
$$=19.138 \text{ degrees} \quad (23)$$

For the receiving end

$$\omega_{R0}=0,\ \delta_{R0}=0$$
$$\omega_{R1}=K_R'(P_{R0}-P_R)t$$
$$=15.710(2.000-0.885)0.1$$
$$=1.752 \text{ radians per second} \quad (22)$$

$$\delta_{R1}=57.3[^1/_2K_R'(P_{R0}-P_R)t^2]$$
$$=57.3[^1/_2\times 15.710(2.000-0.885)0.01]$$
$$=5.018 \text{ degrees} \quad (23)$$

Table I. Summary of Computed Values for Item 5 of Problem Solution

Quantity	Fault On	Fault Cleared	Line Reclosed	Equation
K_S'	62.840	62.840	62.840	2
K_R'	15.710	15.710	15.710	2
e_S	1.045	1.045	1.045	
e_R	1.136	1.136	1.136	
P_{S0}	1.098	1.098	1.098	
P_{R0}	−2.000	−2.000	−2.000	
P_M		1.047	1.400	5
ϕ		−3.2 degrees	−6.0 degrees	6
P_S'	0.033	0.217	0.212	7
P_R'	0.885	2.723	2.644	8
K'		78.471	78.236	10
R		0.813	0.600	11
ψ		−1.9 degrees	−3.6 degrees	13
P_{S0}'		0.881	0.886	14
P_{R0}'		0.723	0.644	15
W		12.818	14.801	16

Note that both machines are treated as sending-end machines for this computation. Since the receiving-end machine actually drops load and speeds up during this period, the relative velocity and angle will be the difference between the two sets of values.

Therefore

$\omega_1 = 6.692 - 1.752 = 4.940$ radians per second

$\delta_1 = \delta_e + \delta_{S1} - \delta_{R1} = 32.3 + 19.1 - 5.0 = 46.4$ degrees

7. From equation 18

$\omega_1' = \omega_1/W = 4.940/12.818 = 0.385$

From equation 12

$\theta_1 = \delta_1 - \psi = 46.4 + 1.9 = 48.3$ degrees

8. Since $R = 0.813$, it will be necessary to interpolate results between the curves for $R = 0.80$ and $R = 0.85$

For $R = 0.80$, $\theta_1 = 48.3$, $\omega' = 0.385$: $C = 1.18$
For $R = 0.85$, $\theta_1 = 48.3$, $\omega' = 0.385$: $C = 1.24$

9. The value of T_1 is found for each value of R

For $R = 0.80$, $\theta_1 = 48.3$, $C = 1.18$: $T_1 = -0.20$
For $R = 0.85$, $\theta_1 = 48.3$, $C = 1.24$: $T_1 = -0.40$

10. Since $T_2 - T_1 = W(t_2 - t_1) = 12.818 \times (21-6)/60 = 3.204$

For $R = 0.80$, $T_2 = -0.20 + 3.20 = 3.00$
For $R = 0.85$, $T_2 = -0.40 + 3.20 = 2.80$

11. For $R = 0.80$, $T_2 = 3.00$, $C = 1.18$: $\theta_2 = 105$ degrees

For $R = 0.85$, $T_2 = 2.80$, $C = 1.24$: $\theta_2 = 108$ degrees

By interpolation, for $R = 0.813$, $\theta_2 = 105.8$ degrees

12. For $R = 0.80$, $C = 1.18$, $\theta_2 = 105$ degrees, $\omega_2' = 0.17$

For $R = 0.85$, $C = 1.24$, $\theta_2 = 108$ degrees, $\omega_2' = 0.22$

By interpolation, for $R = 0.813$, $\omega_2' = 0.183$

13. From equation 12, $\delta_2 = \theta_2 + \psi = 105.8 - 1.9 = 103.9$ degrees

From equation 18, $\omega_2 = W \times \omega_2' = 12.818 \times 0.183 = 2.346$

14. From equation 12 $\theta_2 = \delta_2 - \psi = 103.9 + 3.6 = 107.5$ degrees

From equation 18 $\omega_2' = \omega_2/W = 2.346/14.801 = 0.158$

15. From equation 17

$C = R\theta_2 + \cos\theta_2 - (\omega_2')^2$

$= 0.600 \times 107.5/57.3 - 0.301 - 0.158^2$
$= 0.799$

16. The point $R = 0.600$, $C = 0.799$ is shown as point P on Figure 1. The system is stable with some margin.

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No Discussion

Torque of Reluctance-Type Magnetic Couplings

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MEMBER AIEE

THERE are numerous variations of a class of devices bearing the generic name "magnetic couplings," each of which, in turn, has several subgroups. We shall limit ourselves to one variety, the reluctance type. One form of such a coupling consists of an inner rotor having several teeth with spaces between, and an outer rotor having a like number of teeth and spaces. Figure 1 is a sketch of such a device in which the excitation may come from either a permanent magnet or a current-carrying coil.

Either member, or both, may contain the source of magnetomotive force. The source of the magnetomotive force may be a d-c supply or permanent magnets. We shall discuss both methods of magnetomotive force supply without attempting to arrive at a design of optimum proportions.

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The torque of a coupling can be calculated by means of certain energy relationships. Figure 2 is a portion of Figure 1. Tooth A is on the driving member tooth B is on the driven member. Let

W = energy in joules
L = inductance in henrys
I = current in amperes
ϕ = flux in maxwells
N = turns linked with ϕ
x' = displacement in centimeters
x = displacement in inches

We know that

$$W = \frac{1}{2} LI^2 \tag{1}$$

and that

$$L = \frac{(N\phi)}{I \times 10^8} \tag{2}$$

or

$$W = \frac{1}{2}\left\{\frac{(N\phi)}{I \times 10^8}\right\} I^2 = \frac{1}{2}\frac{(N\phi)I}{10^8} \tag{3}$$

Now suppose that because of a certain load torque on B, A is displaced x' centimeters. This required mechanical energy since

$$W = \int_0^{x'} \frac{f_x dx'}{10^7} \tag{4}$$

where f_x is a force in dynes at x' displacement.

Thus

$$f_x = 10^7 \frac{dW}{dx'} \text{ dynes} \tag{5}$$

or

$$F = \frac{10^7}{981 \times 453.6}\frac{dW}{2.54dx} = 8.85\frac{dW}{dx} \text{ pounds} \tag{5A}$$

where x is now in inches.

If $(D+\delta)$ is the mean air-gap diameter in inches, then the torque is

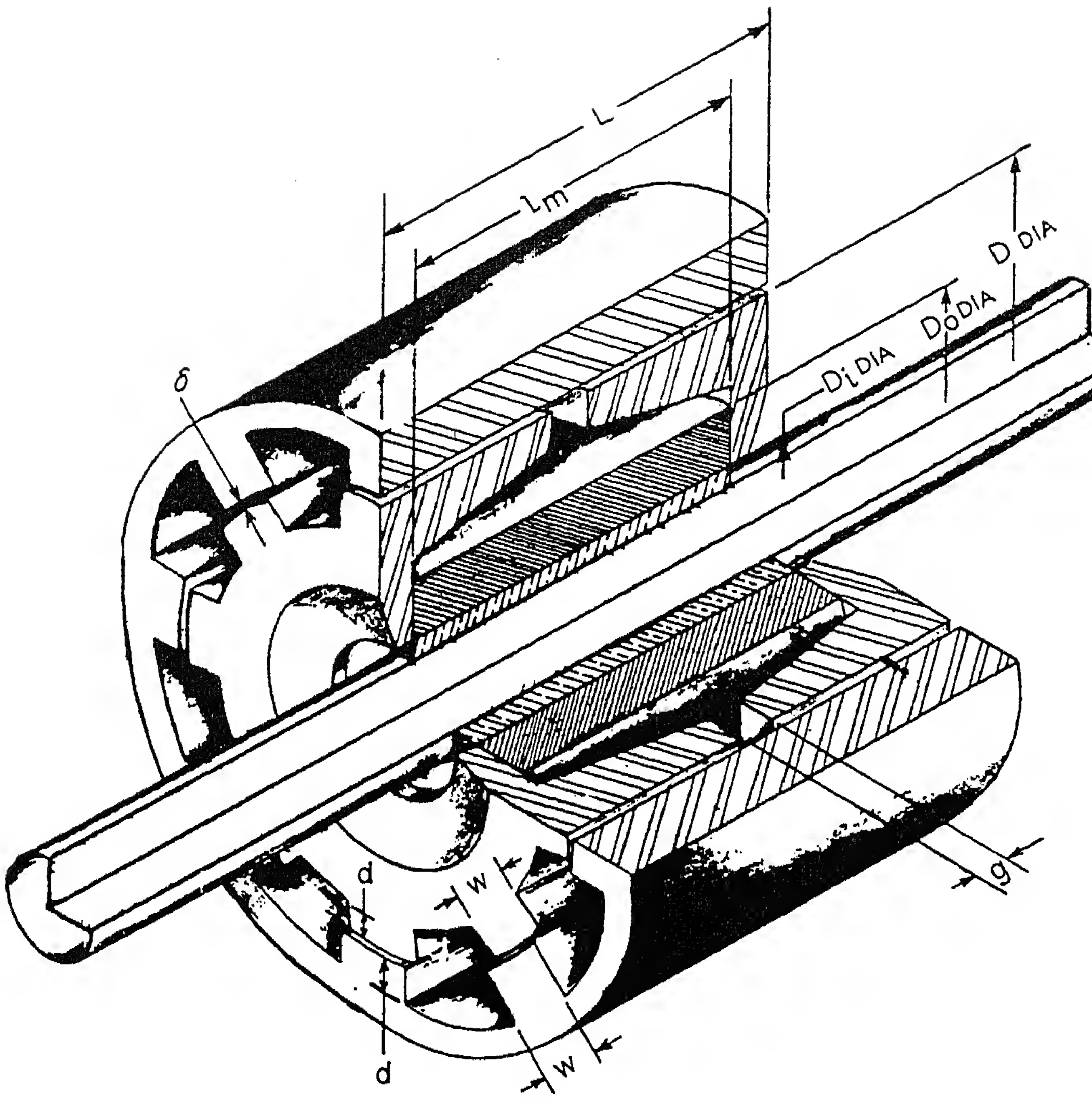
$$T = F\frac{(D+\delta)}{2} \text{ pound-inches} \tag{6}$$

or

$$T = 4.425(D+\delta)\frac{dW}{dx} \text{ pound-inches} \tag{6A}$$

With tooth A displaced x inches from its mating member tooth B, the reluctance of the magnetic circuit has increased, or, conversely, the permeance has decreased. Since

$$\phi = \text{magnetomotive force } P' = \frac{4\pi}{10} NIP' \tag{7}$$



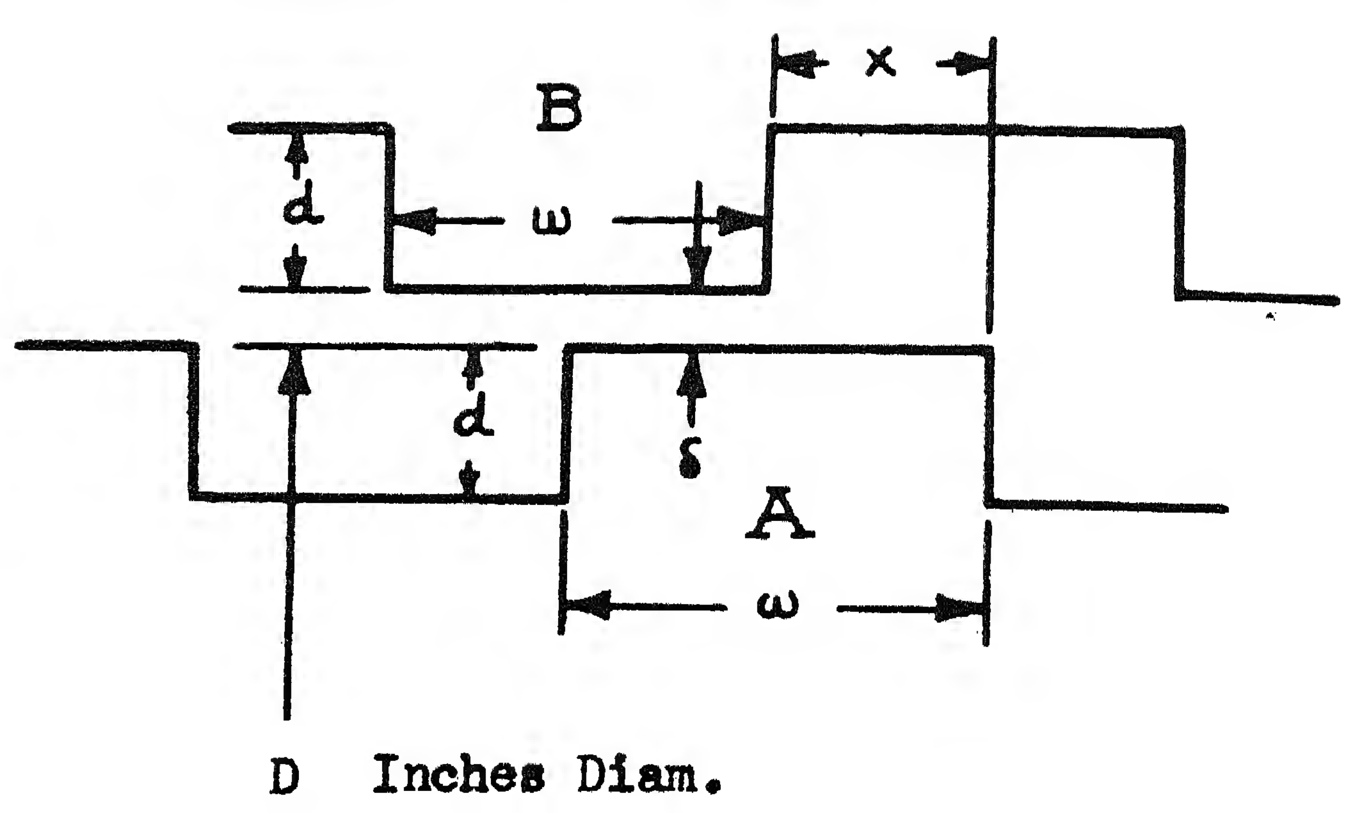


Figure 1 (left). Typical reluctance coupling showing only magnetic and electric elements

Figure 2 (above). Portion of the mating toothed structures with displacement

where P' is the permeance of the magnetic circuit in square centimeters/centimeter and the energy as given in equation 3 becomes

$$W=\frac{NI}{2\times10^8}\,\frac{4\pi}{10}(NI)P'=\frac{2\pi}{10^9}\,(NI)^2P' \text{ joules} \tag{8}$$

If a constant excitation is applied, (NI) is constant, and the energy is proportional to the permeance. Differentiating equation 8 with respect to x

$$\frac{dW}{dx}=\frac{2\pi\times2.54}{10^9}(NI)^2\frac{dP}{dx}$$

$$=\frac{16.0}{10^9}\,(NI)^2\,\frac{dP}{dx} \text{ pounds} \tag{9}$$

where P is the permeance in square inches/inch.

From equations 9 and 6(A) the torque transmitted is

$$T=\frac{70.9}{10^9}\,(D+\delta)(NI)^2\frac{dP}{dx} \text{ pound-inches} \tag{10}$$

Maximum torque occurs at the value of x which makes

$$\frac{d^2P}{dx^2}=0 \tag{11}$$

This all seems very straightforward and simple until we try to evaluate P, dP/dx, and $(d^2P)/dx^2$.

It should be fairly evident that if the magnetic materials are highly saturated, a large portion of the exciting magnetomotive force will be consumed in the iron paths and dP/dx will be less than would be the case if there were little or no saturation.

Let us consider means for determining P as a function of x when there is no appreciable saturation. There are two principal permeance components, the radial and the end or axial.

For determining the radial portion of the permeance, flux plots can be made at different values of x, but this is a long and tedious process. A quicker solution is possible by using a special type of paper having a high resistance conducting coating. The outline of the tooth configuration is painted on using a silver paint. A resistance measurement is made from one member to the other. This resistance is analagous to the reluctance which in turn is the reciprocal of permeance. If a rectangular piece of the conducting paper is used for calibration, the permeance can be evaluated in the desired units.

There are analytical approaches that yield accuracy comparable to this described method. If we let

$$P=(P_A+P_R)N \tag{12}$$

where

P = total permeance in inches²/inch
P_A = axial component of permeance per tooth pitch
P_R = radial component of permeance per tooth pitch
N = number of teeth per member

where

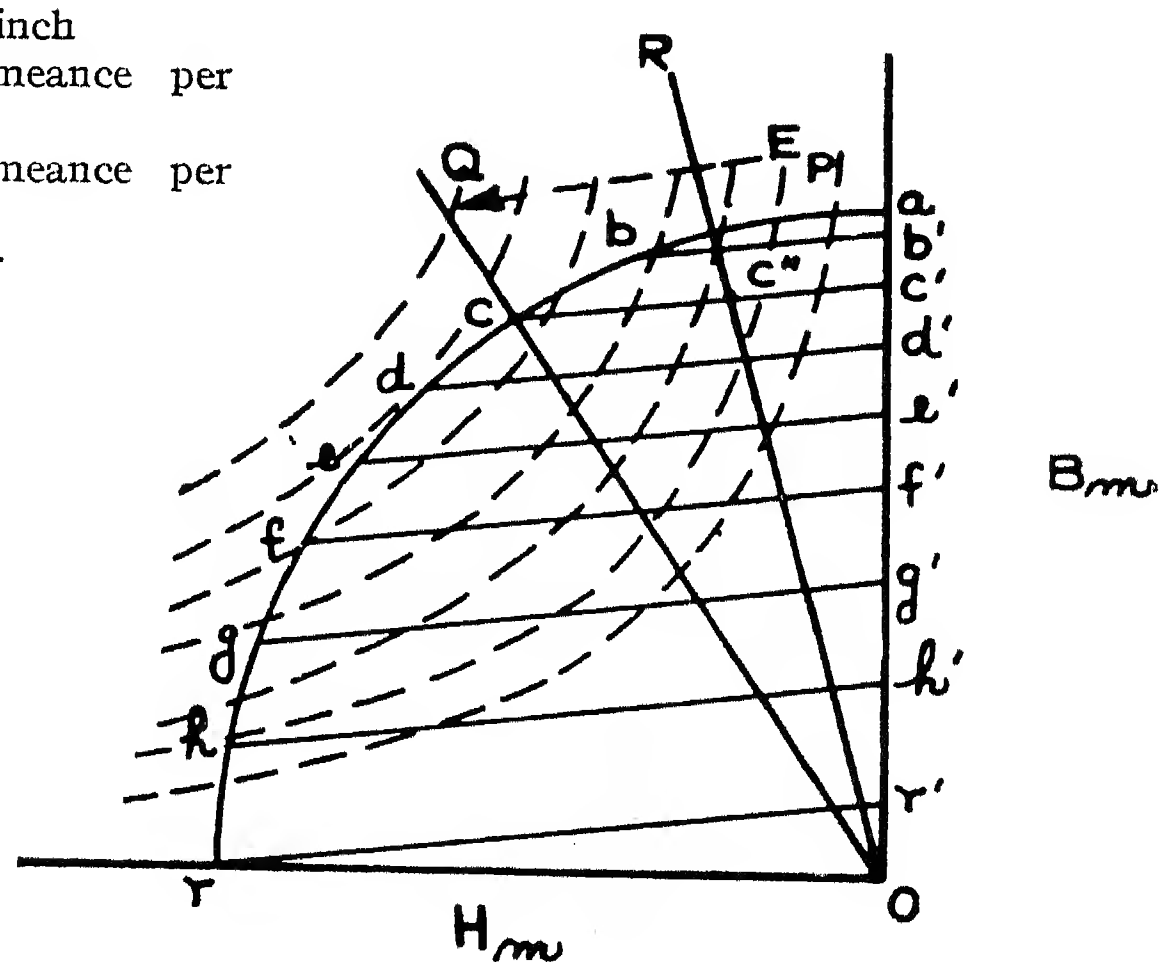


Figure 3. Typical magnetization curve of permanent magnetic material

$$P_A=\frac{2.303}{\pi}\left[\frac{(w-x)}{2}\log_{10}\left\{1+\frac{\pi(d+t)}{\delta}\right\}+x\log_{10}\left\{1+\frac{\pi(d+t)}{\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}}\right\}\right] \tag{13}$$

(see Appendix I for derivation)

$$P_R=\frac{(w-x)(L-g)}{4\delta}+\frac{2.303}{\pi}\times\left[(L-g)\left(\log_{10}\left\{1+\frac{\pi x}{2\delta}\right\}+\log_{10}\left\{1+\frac{\frac{\pi}{2}d}{\delta+d}\right\}\right)+\frac{(w-x)}{2}\times\log_{10}1+\frac{\pi g}{4\delta}\Big)\right] \tag{14}$$

(see Appendix II for derivation)

When permanent magnet excitation is employed, the efficiency of the device reaches its ultimate. Design considera-

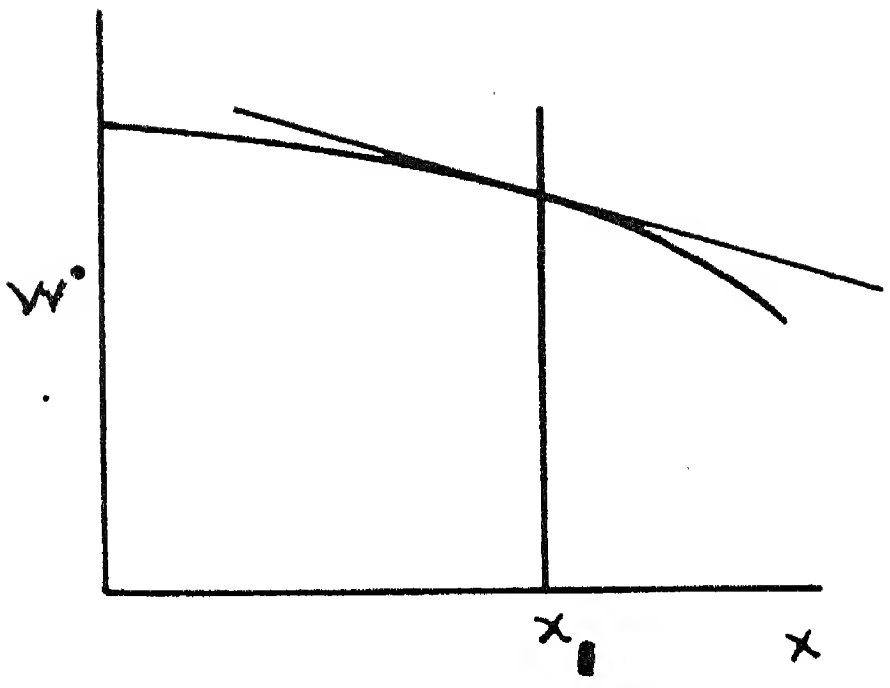


Figure 4. Energy versus displacement

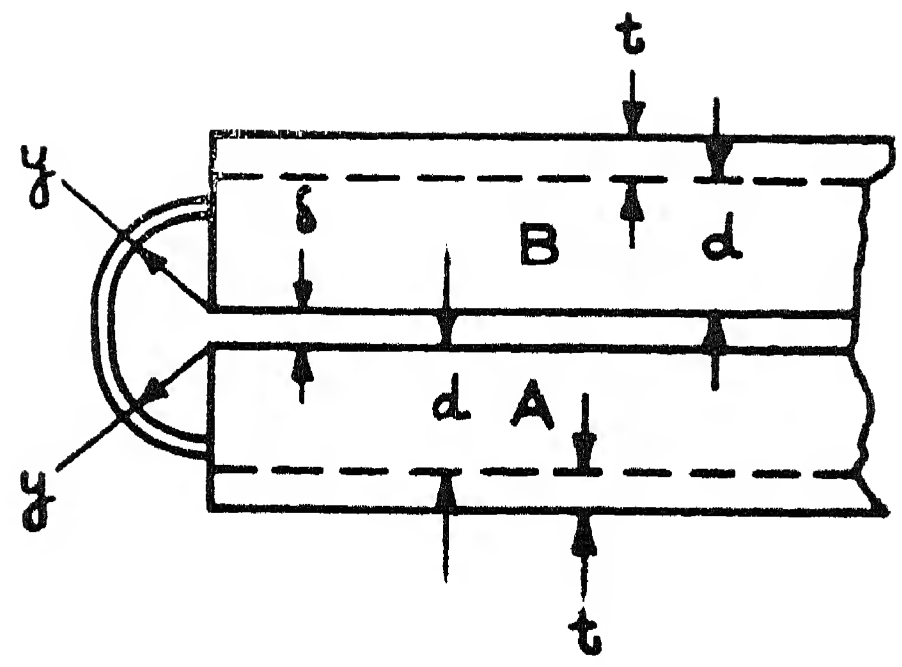


Figure 5. End permeance paths

tions for permanent magnet excitation involve:

1. Type of magnet material.
2. Minimum weight to meet torque specification.
3. Operating minor loop—a function of the air-exposed excited member alone.
4. Avoiding forced demagnetization.

Generally speaking, it is desirable to operate the magnet (or magnets) at high energy levels. From a practical manufacturing and service consideration it is also desirable that the exciting member be shaped so that later handling or disassembly will cause no appreciable deterioration of its initial quality. There have been a number of technical papers published which show the need for parasitic permeance paths to obtain the best operation.

If the rotor shown in Figure 1 is magnetized axially in a very strong field, upon removal from the magnetizer it will retain a flux density level in itself that is a function of the external permeance as well as the inherent qualities of the magnet material.

Rather than deal with the many complications involved in generalization, the discussion will be limited to a specific form of a permanent magnet excitation coupling. A hollow cylinder form of magnet material is used as shown in Figure 1. That the flux in the magnet is equal to the flux external to the magnet is axiomatic.

$$\phi = B_m A_m' \tag{15}$$

where

ϕ = flux in maxwells
B_m = magnet flux density in gausses
A_m' = magnet area in square centimeters $= \pi/4(D_0^2 - D_i^2)\ 2.54^2$

$$H_m = \frac{0.4\pi NI}{l_m'} \tag{16}$$

where

H_m = magnetomotive force in oersteds
l_m = magnet length, centimeters = 2.54 l_m

or

$$NI = \frac{l_m'}{0.4\pi} H_m \tag{16A}$$

Then combining equations 3, 15, and 16(A) we have

$$W = \frac{1}{2\times 10^8}(B_m A_m')\left(\frac{l_m' H_m}{0.4\pi}\right) = \frac{(B_m H_m)(A_m' l_m')}{8\pi\times 10^7} \tag{17}$$

Since

$A_m' l_m' = V_m'$ the volume in cubic centimeters of the magnet
$B_m H_m = E_P$ the "energy product"

$$W = \frac{V_m' E_P}{8\pi\times 10^7} \tag{17A}$$

A typical magnet curve is shown in Figure 3. Not only is the outline for a completely closed magnet circuit shown (a, b, c, d, e, f, g, h, r,) but the important minor loops bb', cc', dd', ee', ff', and so forth, are included, as well as lines of constant energy product. The position of line OQ is a function of the external permeance of the exciting member before assembly with its mating member.

$$\phi = P_0' H_m l_m' = B_m A_m' \tag{18}$$

where P_0' = external permeance in square centimeters/centimeter.

$$B_m = H_m\left(\frac{P_0' l_m'}{A_m'}\right) = H_m \frac{(2.54P_0\times 2.54 l_m)}{2.54^2 A_m} = H_m\left(\frac{P_0 l_m}{A_m}\right) \tag{19}$$

For purposes of determining the line OQ, H_m can be assumed to be anything.

P_0 can be approximated to a close degree with this equation.

$$P_0 = N[P_\alpha + P_\beta + P_\gamma] \tag{20}$$

where

$$P_\alpha = w_d/g \tag{21}$$

$$P_\beta = \frac{4.606}{\pi} d \log_{10}\left(1 + \frac{\pi d}{2g}\right) \tag{22}$$

$$P_\gamma = \frac{4.606w}{\pi} \log_{10}\left(1 + \frac{\pi l_m}{2g}\right) \tag{23}$$

(see Appendix III for derivation)

For the particular case chosen we are at point c. If the magnetized inner rotor is assembled into an unmagnetized outer member with the teeth exactly matching, the permeance is greatly increased and we find we are at point c'' on the minor loop cc'.

In the case of d-c excitation leakage permeances, such as at gap g, were of no significance if the steel parts were not permitted to saturate appreciably since this component of permeance is constant. In the permanent magnet excitation case this is no longer true since the excitation is actually a function of the flux density in the magnet. If saturation of the steel parts is neglected, the permeance when assembled becomes very nearly

$$P_T = N[P_A + P_R + P_\alpha + P_\beta + P_\gamma/2] \tag{24}$$

The line OR on the magnetization chart is obtained from

$$B_m = H_m\left(\frac{P_T l_m}{A_m}\right) \tag{25}$$

which is similar in form to equation 19.

The line OR intersects the minor loop cc' at point c'' thereby determining H_m, B_m, and their energy product E_P. For

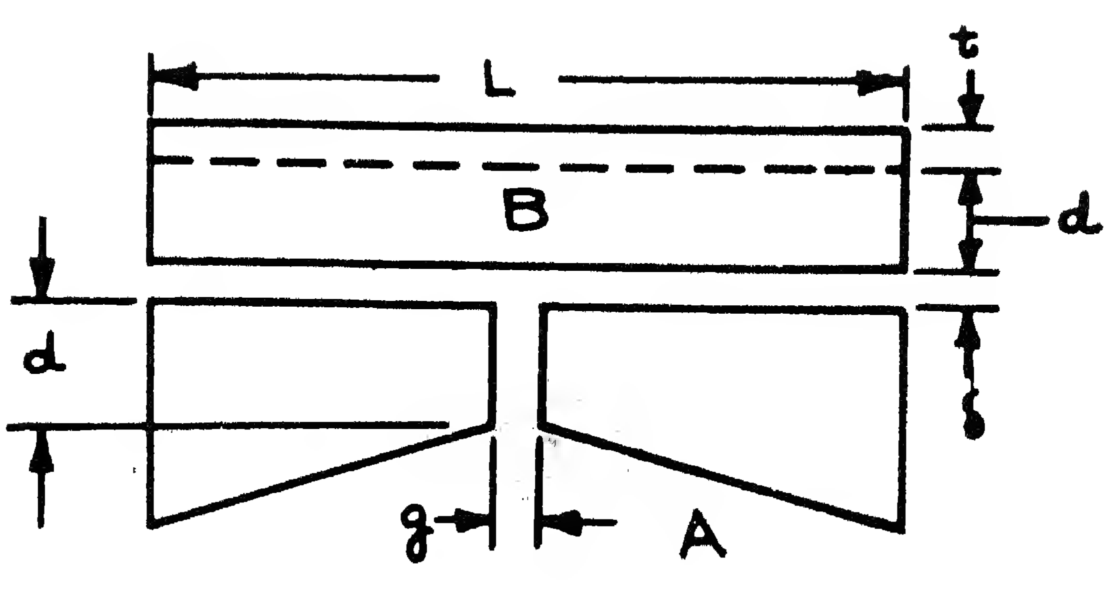


Figure 6 (left). Radial permeance paths

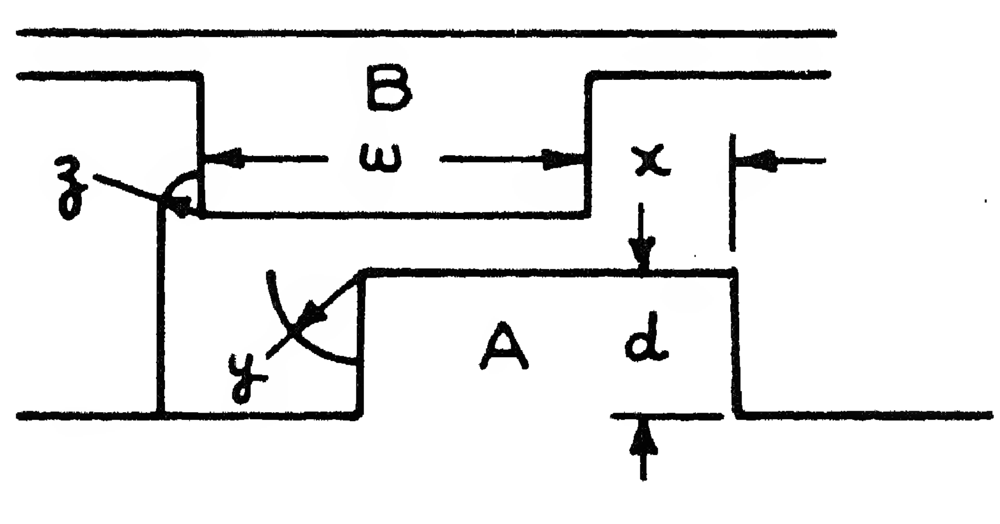


Figure 7 (right). Radial permeance paths

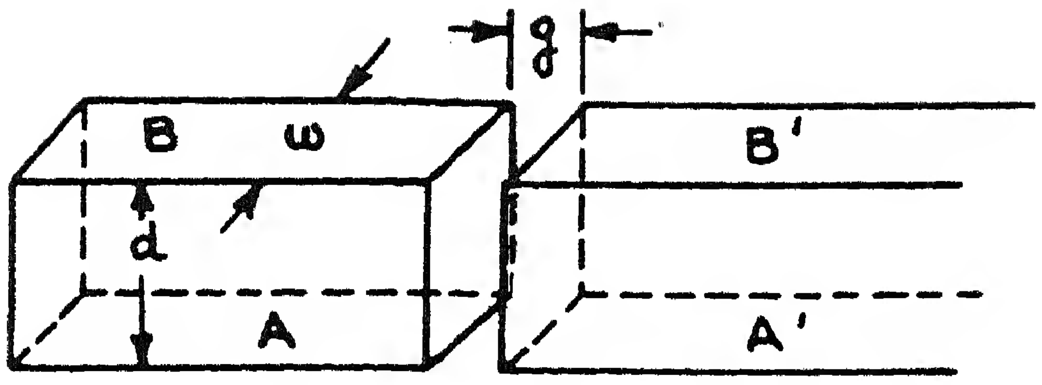


Figure 8. "Exposed" inner rotor permeance paths

different values of displacement, x, with the resulting permeance changes since $P_T=f(X)$, the several positions of line OR are determined, and with them the corresponding B_m, H_m, and E_P values. In equation 17(A) we have shown the energy is directly proportional to E_P. By plotting W versus x, Figure 4, we can obtain dw/dx by drawing a line tangent to the curve at x, and thus determine the torque from equation 6(A).

Appendix I. Axial Components of Permeance

Imagine a permeance tube emerging axially from member A and entering member B axially. It will be assumed that the permeance tube is as shown in Figure 5, consisting of two quarter circles of y radius and a straight part δ long. P_{A1} will be designated as this component. This permeance tube will have a length of $\{\delta+2(\pi/2y)\}$ and a section $(w-x)dy$, and since there are two ends in series

$$P_{A1}=\frac{(w-x)}{2}\int_0^{(d+t)}\frac{dy}{\delta+\pi y}$$

$$=\frac{(w-x)}{2\pi}\Big[ln(\delta+\pi y)\Big]_0^{d+t}$$

$$=\frac{(w-x)}{2\pi}\,ln\left\{1+\frac{\pi(d+t)}{\delta}\right\}$$

The limits $\Big|_0^{d+t}$ are arbitrary.

Now imagine a permeance tube emerging axially from A and entering B axially. It will be assumed that it has an average length of $\left\{\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}+2\left(\frac{\pi}{2}z\right)\right\}$ and a section xdz, and since there are two of these per tooth and again two ends are in series

$$P_{A2}=\frac{(2x)}{2}\int_0^{(d+t)}\frac{dz}{\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}+\pi z}$$

$$P_{A2}=\frac{x}{\pi}\left[ln\left(\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}+\pi z\right)\right]_0^{d+t}$$

$$=\frac{x}{\pi}\,ln\left\{1+\frac{\pi(d+t)}{\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}}\right\}$$

$$P_A=P_{A1}+P_{A2}=\frac{2.303}{\pi}\times\left[\frac{(w-x)}{2}\log_{10}\left\{1+\frac{\pi(d+t)}{\delta}\right\}+x\log_{10}\left\{1+\frac{\pi(d+t)}{\sqrt{\left(\frac{x}{2}\right)^2+\delta^2}}\right\}\right]$$

This is the total axial permeance.

Appendix II. Radial Components of Permeance

The radial permeance (P_R) may be divided into several parts; see Figures 6 and 7. The first part concerns those permeance tubes whose length is δ and whose section is $(w-x)\left(\frac{L-g}{2}\right)$. Two sections are in series

$$P_{R1}=\frac{(w-x)(L-g)}{4\delta}$$

Imagine permeance tubes emerging from A having a length $(\pi/2y+\delta)$ and a section $\left(\frac{L-g}{2}\right)dy$. There will be two of these parts for each tooth with two sections in series.

$$P_{R2}=\frac{(L-g)}{4}\,2\int_0^x\frac{dy}{\delta+\pi/2y}$$

$$=\frac{(L-g)}{\pi}\Big[ln(\delta+\pi/2y)\Big]_0^x$$

$$=\frac{(L-g)}{\pi}\,ln\left(1+\frac{\pi/2x}{\delta}\right)\text{ for } x<d$$

Similarly

$$P_{R3}=\frac{(L-g)}{4}\,2\int_0^d\frac{dz}{(\delta+d)+\frac{\pi}{2}z}$$

$$=\frac{(L-g)}{\pi}\left[ln\left\{(\delta+d)+\frac{\pi}{2}z\right\}\right]_0^d$$

$$=\frac{(L-g)}{\pi}\,ln\left(1+\frac{\frac{\pi}{2}d}{\delta+d}\right)$$

$$P_{R4}=\frac{(w-x)}{4}\int_0^{g/2}\frac{dq}{\delta+\frac{\pi}{2}q}$$

$$P_{R4}=\frac{(w-x)}{2\pi}\left[ln\left(\delta+\frac{\pi}{2}q\right)\right]_0^{g/2}$$

$$=\frac{(w-x)}{2\pi}\,ln\left(1+\frac{\frac{\pi}{4}g}{\delta}\right)$$

$$P_R=P_{R1}+P_{R2}+P_{R3}+P_{R4}$$

$$=\frac{(w-x)(L-g)}{4\delta}+\frac{2.303}{\pi}\times\left[(L-g)\left\{\log_{10}\left(1+\frac{\pi x}{2\delta}\right)+\log_{10}\left(1+\frac{\frac{\pi}{2}d}{\delta+d}\right)\right\}+\frac{(w-x)}{2}\times\log_{10}\left(1+\frac{\pi g}{4\delta}\right)\right]$$

Appendix III. Permeance of Magnetized Member Alone

Two rectangular surfaces (Figure 8), separated by a uniform gap g, have an area wd.

$P_d=w_d/g$

Imagine permeance tubes leaving surface A (at right angles) and terminating on surface A' (at right angles). They will have an area ddy assuming their length as $\left[g+2\left(\frac{\pi}{2}y\right)\right]$ and remembering they exist on each side of the tooth, then

$$P_\beta=2\int^{d/2}\frac{ddy}{g+\pi y}=\frac{2d}{\pi}\Big[ln(g+\pi y)\Big]_0^{d/2}$$

$$=\frac{4.606d}{\pi}\log_{10}\left(1+\frac{\frac{\pi}{2}d}{g}\right)$$

The limits $\Big|_0^{d/2}$ are arbitrary and apply to the case when the space between teeth $>2d$. If the space between teeth $<2d$ use limits of $\Big|_0^{1/2}$ space between teeth.

Imagine permeance tubes leaving surface B (at right angles) and terminating on surface B' (at right angles). They will have an area wdq. Assuming their length as $g+2\left(\frac{\pi}{2}q\right)$ and remembering this exists on each side

$$P_\gamma=2\int_0^{lm/2}\frac{wdq}{g+\pi q}=\frac{2w}{\pi}\Big[ln(g+\pi q)\Big]_0^{lm/2}$$

$$=\frac{4.606w}{\pi}\log_{10}\left(1+\frac{\pi lm}{2g}\right)$$

The limits $\Big|_0^{lm/2}$ are arbitrary.

No Discussion

Some Observations on the Economic Benefits in Going from One System Voltage Level to a Higher System Voltage Level

D. K. BLAKE
FELLOW AIEE

VOLTAGE is the great liberator of electric transmission and distribution systems. The ability to increase system voltages economically permits systems to grow. Voltage increase and load growth are mutually dependent companions. They help each other. One could not get very far without the other. It is the growing load calling for higher voltages that challenges the long range system planner. What voltage shall I use? Shall I use the one I have now or some higher one? How many voltage levels shall I have? The answer to these questions is not always determined finally on economic grounds. Practical consideration of operation, maintenance, safety, clearance, trees, and outages serve to make the problem a highly complex one.

Scope

The scope of this paper will be limited to relative economic benefits in terms of load, distance, or area. Conductor size will be kept constant from one voltage to another since it is assumed that there is always enough load to utilize fully the same copper size used at the lower voltage. The objective will be to obtain a better understanding of the benefits to be expected so that one will be in a better position to evaluate the practical intangibles. The observations will be limited to overhead systems since they get maximum benefits because they are usually limited in load-carrying ability by the permissible voltage drop. Understanding comes with observing ideal geometrical patterns and uniform load distribution. This is that kind of paper.

Twin Benefits of Voltage

Increasing to a higher voltage can be economically justified in two ways: 1. carrying more load, or 2. covering more distance. If the voltage is doubled, the current of a given load will be cut in half. In the same line resistance and reactance this half-current will give half-voltage drop which divided by twice the voltage will be one-quarter in per cent of the voltage drop of the original circuit. In a similar way half-current square gives one-quarter the per-cent copper loss. This is the famous voltage-square rule and is the basis of all economic evaluation of increasing overhead system voltages. Cable systems being thermal or current limited do not lend themselves so readily to increased voltage benefits. So doubling the voltage in overhead systems will permit four times the load the same distance or the same load four times the distance. The per-cent voltage drop and per-cent copper loss is the same in either case.

Distributed Loads a Special Case

The foregoing statement is satisfactory for transmission of power from one point to another, but in distribution systems it is restricted in its application. In distribution systems, the general rule is that load and distance go hand in hand. For a given load density, a longer line picks up more load. So in the case of distributed loads, we must think of the voltage square factor of 4 as being divided between distance and load. Two times the distance will pick up two times the load and have the same per-cent voltage drop and same per-cent copper loss. As long as any distance times any load equals four, the per-cent voltage drop and per-cent copper loss are equal. If the voltage were increased to three times, the voltage-square factor would be 9 and carry three times the load three times the distance. So any voltage ratio squared is thought of as being equal to load times distance.

The Different System Voltage Levels Are Intimately Related

Our distribution systems have two or more voltage levels, the higher one supplying the lower one through transformers. If the voltage of one of these is raised, its economic relation to the other is changed and strict economy would tend to require a change in the voltage level of the other. There is one level, however, that is not changed, economical or not, and that is the domestic utilization voltage of 120/240 volts. Fear of safety to the consumer has been the decisive factor in this country although other countries use 240 volts for lighting. Nevertheless, a few observations will be made to see what a 240/480-volt system might look like and how it would be related to the primary voltage that supplies its transformers.

Secondary System Could Benefit From Double Voltage

The simplest approach is to keep the copper size constant. Then doubling the voltage will mean double the distance between transformers which in turn means double load and hence double the kilovolt-ampere size of transformers, see Figure 1. The principal benefit of this will be to reduce the number of transformer locations to one-half. This will give lower transformer cost per kilovolt-ampere and lower transformer losses per kilovolt-ampere on account of the larger rating, a benefit that is a result of increase in size. In addition, we have half the cutouts and lightning arresters. An additional benefit comes in the form of larger motors that can be started from the viewpoint of disturbing lamp flicker. Figure 1 indicates a case where 20 amperes gives the limit at 120 volts and corresponds to a one-quarter-horsepower motor. The larger transformer permits 24 amperes instead of 20 amperes at 240 volts which corresponds to a three-quarter-horsepower motor starting through twice the distance of secondary line.

240/480 Volts Secondary Helps Economy of 12 Kv Over 4 Kv

Now this reduction in the number and the increase in individual size of transformers resulting from doubling the secondary system voltage has created a new situation with respect to the primary voltage. This new condition tends to favor a higher primary voltage so that it may be increased economically also. Suppose the primary is 2,400/4,160 volts and comparison is made with 7,200/12,480 volts. If the secondary is 120/240 volts the 12-kv class will cost about $6.40

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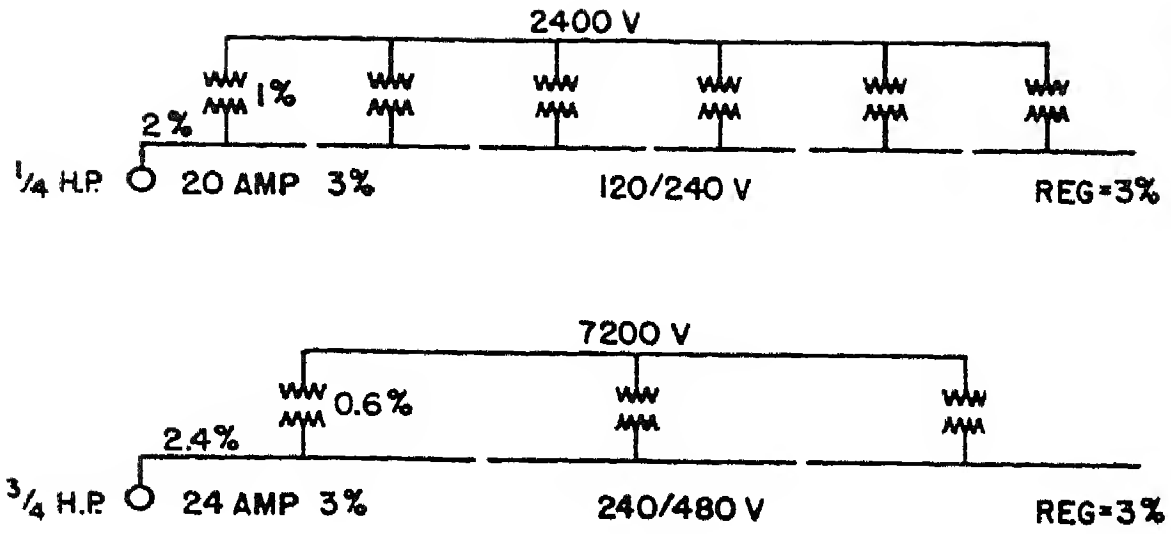
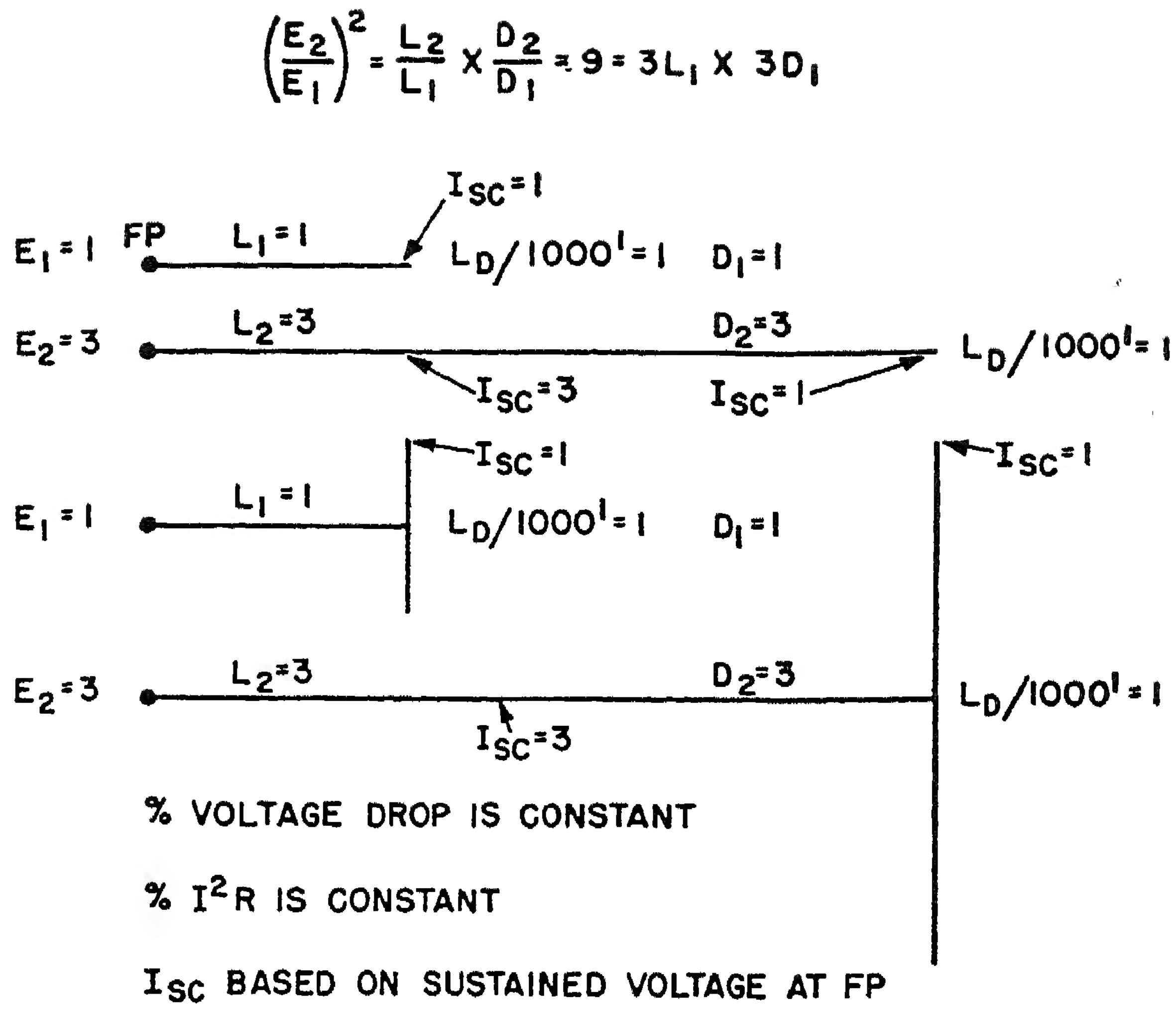


Figure 1 (above). Double secondary voltage reduces number of transformers and makes more favorable a higher primary voltage. Permissible starting current substantially increases permissible motor size

Figure 3 (right). $(E_2/E_1)^2$ is called voltage square factor. Where simple patterns do not result in area coverage, the voltage square factor can be expressed as a simple product of distance and load. Short-circuit current I_{SC} is constant at corresponding points for either voltage



per kilovolt-ampere more for a 10-kva rating where the curve in Figure 2 shows a 20-kva unit (not a standard size, of course) to go with double the voltage would cost only $4.10 per kilovolt-ampere more. Doubling the secondary voltage does not increase the cost of the transformer itself. This $2.30 is a substantial gain for the 12-kv system which gives it a better chance of favorable comparison with the 4-kv system. So doubling the secondary voltage does favor a higher primary voltage and the two voltages are therefore economically related. In terms of feeder loads, these dollar values are increased, say 50 per cent, by diversity.

Let us look now at the primary voltage. A good comparison would be 4 kv to 12 kv because these two are very common and they give a convenient ratio of three. Our voltage-square factor becomes 9 which means we can go three times the distance and pick up three times the load, other things being equal. This relation applies to a straight line feeder, to one terminating in the middle of short or long branches as seen in Figure 3 just so long as load and distance are directly related.

"Distance Coverage" and "Area Coverage" Convenient Distinctions

At this point two distinctions must be made. They are "distance coverage" and "area coverage." Figure 3 is distance coverage whereas Figure 4 is area coverage in a sense because the laterals or branches give the two dimensions of length and breadth to the circuit. However, we can still think of the simple relationship of 3×3 because one of the dimensions is constant and only one varies. In such cases the area coverage is proportional to the length or distance and hence also the load so that for a given uniform density there would be three times the area and consequently three times the load, three times the length or distance. Such examples do occur in practice where restrictions prevent extension laterally. They may also be thought of as long rural lines through a valley with relatively short branches.

When, however, the breadth changes in the same ratio with the length, the situation arises where the area and consequently the load increases with the product of the length and the breadth. If the length and the breadth each increase in the same ratio, then the area and the load increase as the square of the ratio of the increase in the dimensions or distance. Thus it is not possible to increase the length of each line three times because this would result in an area nine times as large which at the same density would be nine times the load and thereby exceed the allowable voltage drop and probably a thermal limit too because we would then have three times the current we had at the lower voltage. However, the dimensions could be increased three times and the area nine times if the load were limited three times. This is another way of saying that three times the voltage enables us to carry three times the load over nine times the area at one-third the density as illustrated in Figure 5. It only complicates matters to change the density with voltage so Figure 5 is not of much help at the moment. It merely shows that the simple relationship of three times three when both the length and the breadth are increased can no longer be used. Another factor for load and distance must be sought.

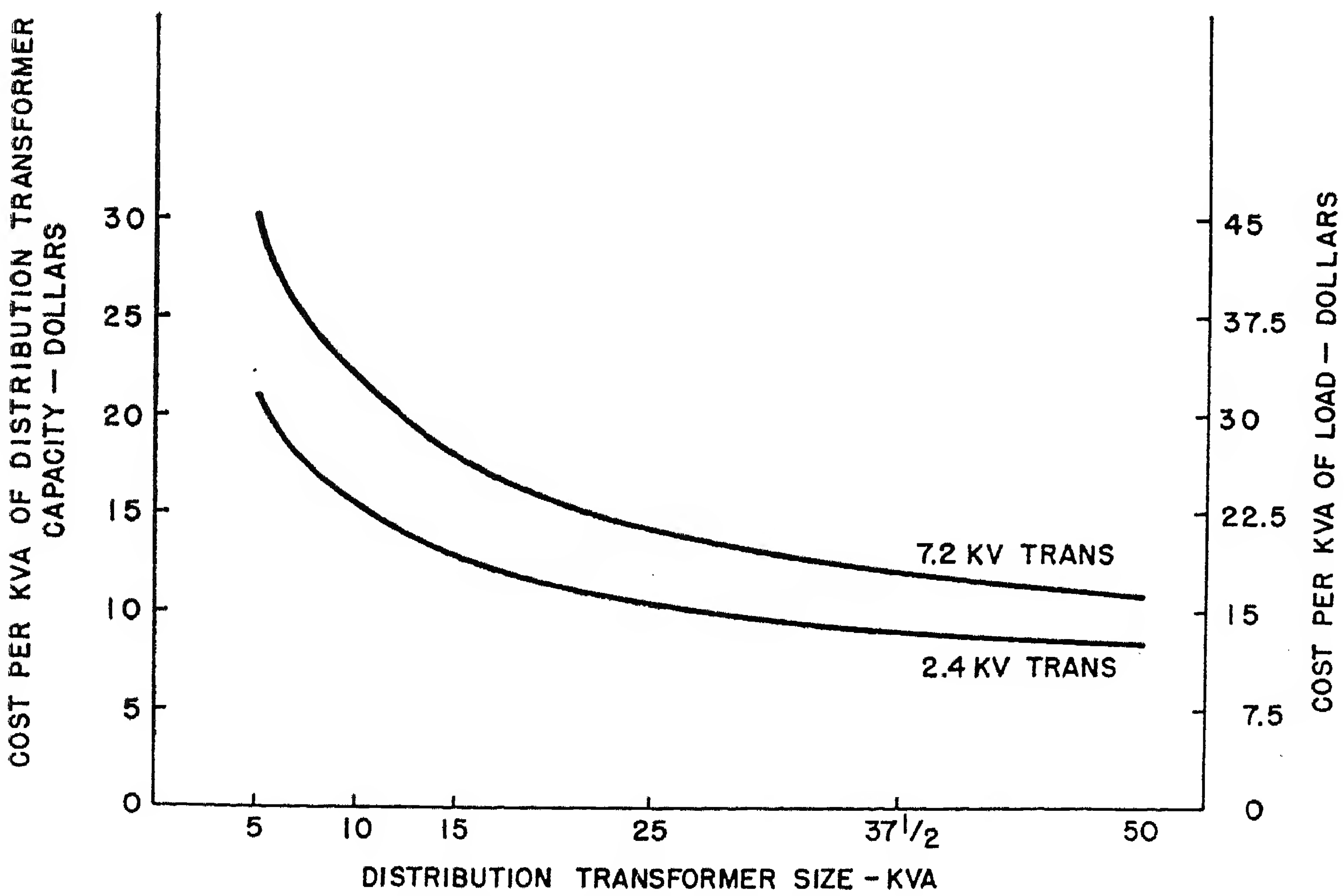


Figure 2. Cost of distribution transformer capacity including one lightning arrester and cutout. Voltage charge for distribution transformers diminishes with increasing size. In terms of feeder loads, dollar values increase, say 50 per cent, in proportion to diversity as shown by right-hand values

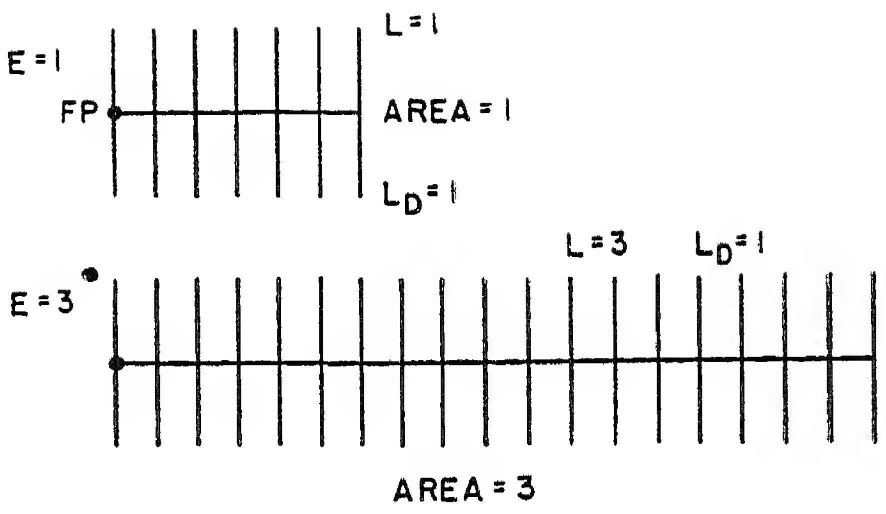


Figure 4. Even area coverage can be expressed by simple product of load and distance provided only one dimension varies and the load directly with it

Triple Voltage Supplies 4.33 Times Load Over 4.33 Times Area

This is done by putting a new meaning into distance factor D. Instead of simple length it now comes to mean dimensions. That is, the dimensions of length and breadth will be changed by factor D. D must now be found when length and breadth increase in the same ratio. Now length a times breadth b gives ab the area. The load L is proportional to area ab. If each a and b is multiplied by D the new area and hence the new load L becomes D^2ab. Since the voltage drop is the product of load L times distance D, the new LD becomes D^3ab. Now this is to be equated to the voltage square factor which is 9 so that $D^3ab=9$. Since it is assumed that the original area at 4 kv was equal to unity then $D^3=9$ and therefore $D=\sqrt[3]{9}=2.08$. So the new area and consequently the new load becomes $2.08a \times 2.08b = 4.33\ ab$ or simply 4.33 times the original load flowing over a length of line 2.08 times as long as before. This result is illustrated in Figure 6. So this now can be put in an imposing formula and it can be said that the new distance D when going from a lower voltage E_1 to a higher voltage E_2 is $D=\sqrt[3]{E_2^2/E_1^2}$ or the cube root of the voltage ratio squared.

Thus a matter of great importance in distribution and subtransmission is arrived at. By tripling the voltage, 4.33 times the area can be covered and then 4.33 times the load can be carried. Now it does not matter what the geometry of the area is just so long as the two areas have the same geometry which, of course, may not work out in practice. Nevertheless, if the area is large and fairly contiguous the departures may not be too great.

Feeder Areas and Substation Areas Benefit Alike

Neither does it matter whether feeder areas or substation areas are considered. Figure 7 shows a large 4-kv substation of 16 feeders. With 12 kv the number of circuits can be reduced to four but each one could carry 4.33 instead of four times the load so the area and load for each circuit, and consequently the substation area, increases in the same ratio $4.33/4.00 = 1.08$ times. In this case the area coverage of the feeders gets the benefit of 4.33×2.08, but the distance coverage between the feeder area and the substation does not because the longest 12-kv feeders are shorter than the longest 4-kv feeders. Increasing the permissible feeder length by 2.08 the longest 4-kv feeder will give a substation area and load equal to 4.33.

Twelve-Kv Distribution Helps 69-Kv Subtransmission Over 34.5 Kv

So, to get full benefits from the higher voltage the substation area and load should be increased 4.33 times as shown in Figure 8. When this is done the situation on the subtransmission side is changed just as was done when we went from 240 volts to 480 volts on the secondary system. By going to 12 kv the number of substations is reduced so that there are 1/4.33 as many. If the 4-kv substations were supplied from 12 kv it would be a simple case of eliminating a transformation and no change in voltage levels above 12 kv would need to follow. If the 4-kv substation were fed at 34.5 kv, the question arises whether 34.5 kv would continue to be the economical voltage with the load and area 4.33 times as large. Figure 9 shows the 12 kv with one substation rated 4.33 and the 4 kv with four substations rated unity for a total of four. The 12-kv substation presents a simple distance coverage problem between the choice of 34.5 kv and some higher voltage like 69 kv. If the latter cost 1.5 times the

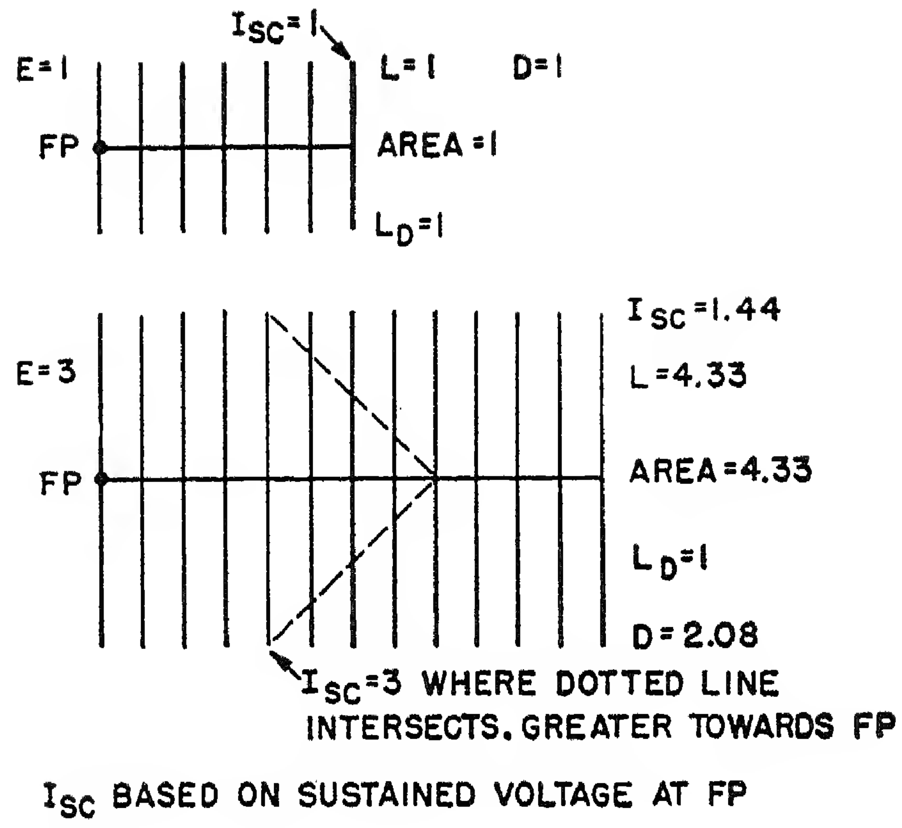


Figure 6. If both dimensions change then the area changes. The load changes corresponding to area change which in turn is proportional to the square of the ratio of the dimensions. This load times the cube root of the voltage square factor gives same per-cent voltage drop and loss. Short-circuit current increases with voltage because increase in distance is less than the increase in voltage

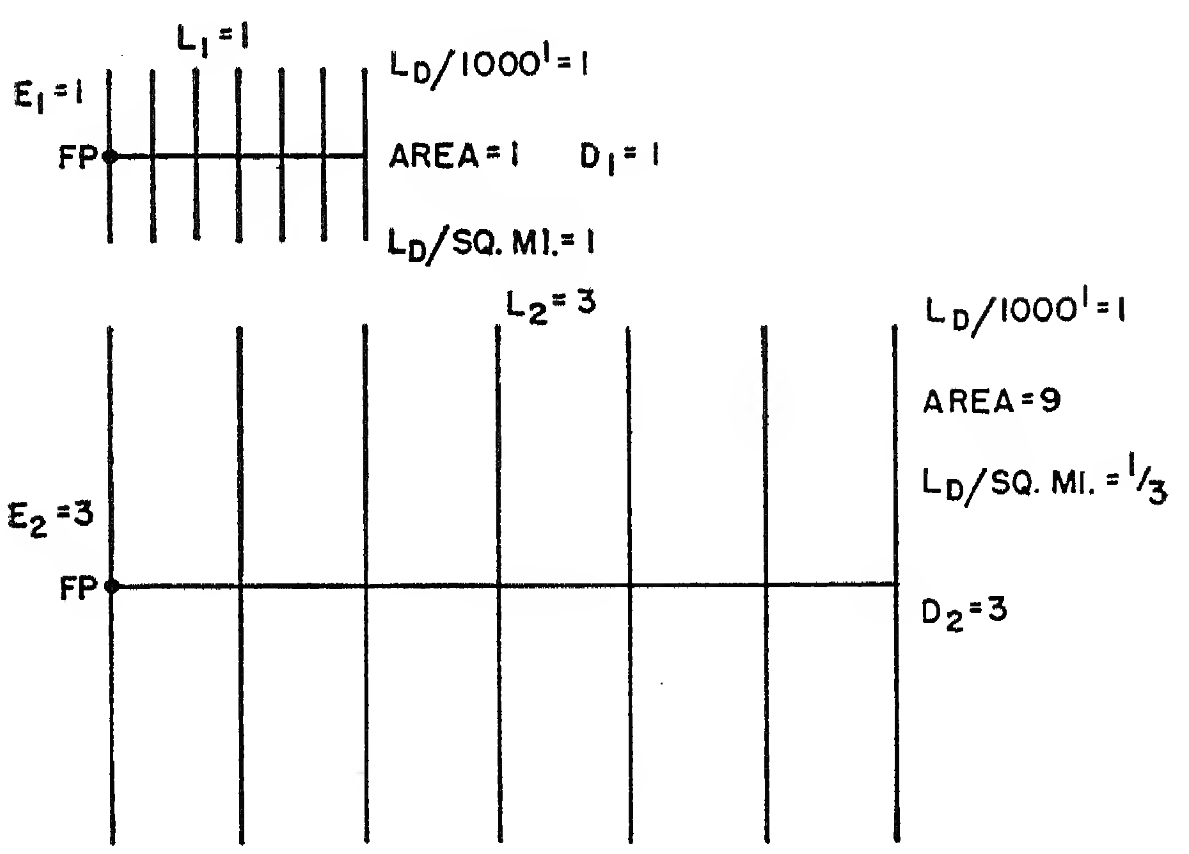


Figure 5. If the voltage square factor is taken and all dimensions change in the same proportion, the load cannot be increased at all but must be decreased. This results in a lighter load density requiring lighter loadings

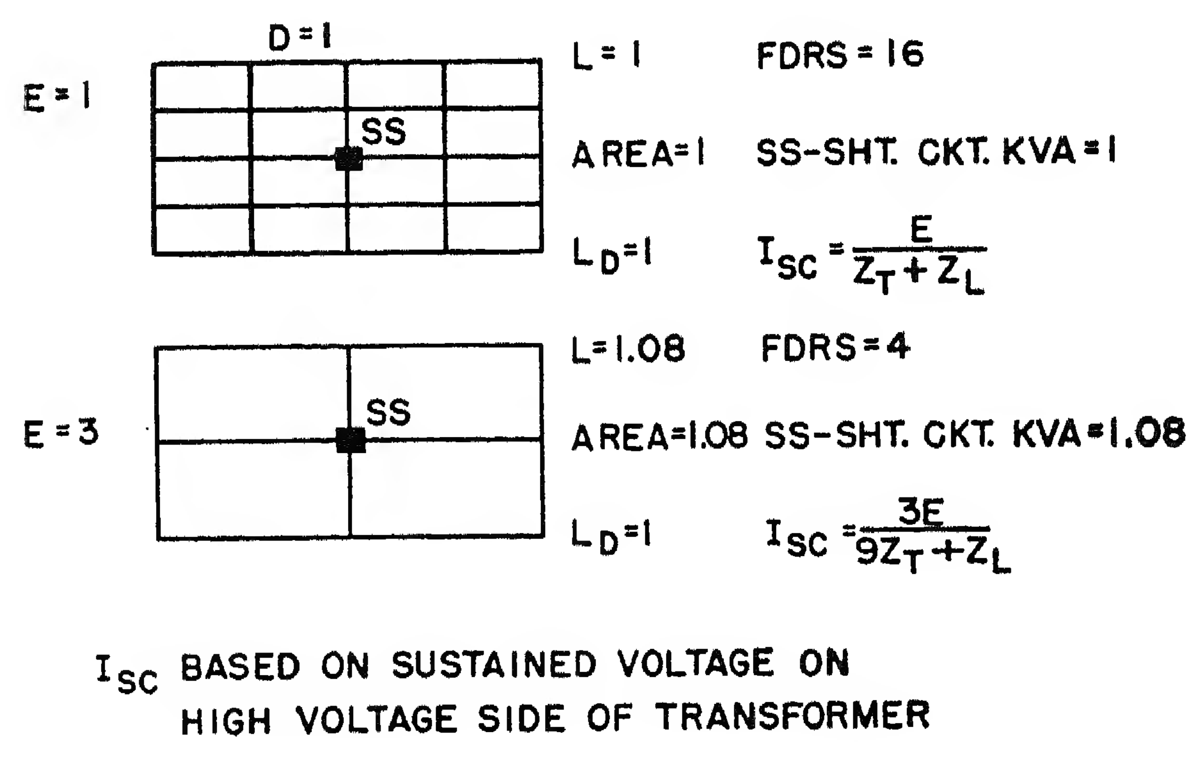
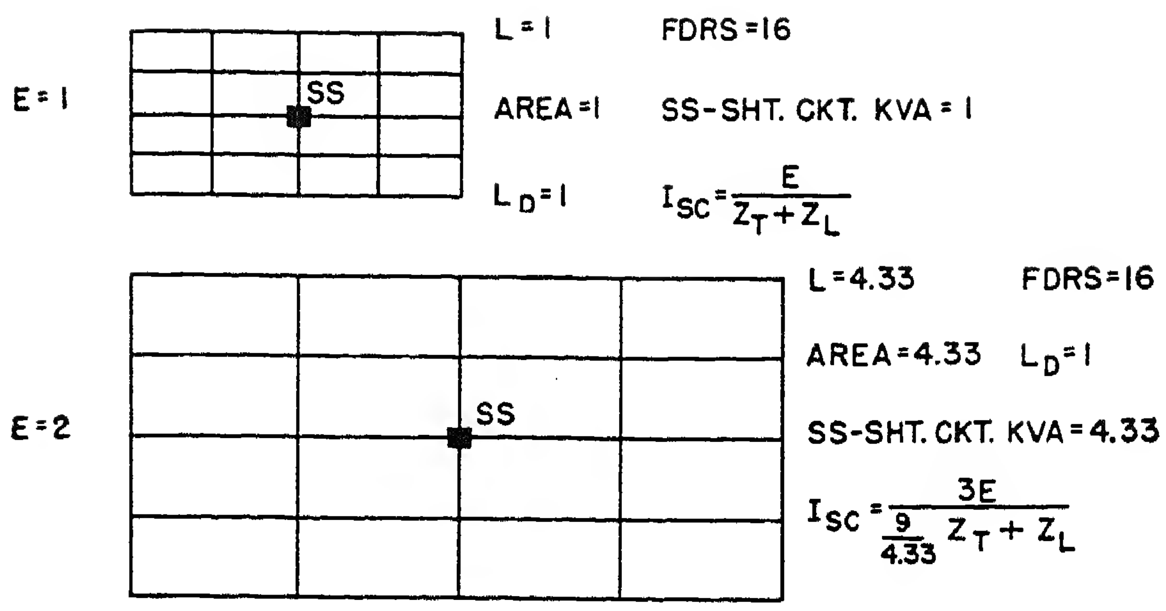


Figure 7. Instead of 16 feeders a substation would require only four and each in turn could carry 4.33 instead of four times the load. Hence substation size would be $4.33 \div 4 = 1.08$. Transformer impedance in ohms substantially limits the short-circuit current to a lower value at the higher voltage



I_{SC} BASED ON SUSTAINED VOLTAGE ON HIGH VOLTAGE SIDE OF TRANSFORMER

Figure 8 (left). Each feeder can carry increased load and the longest feeder can be in the ratio of the cube root of the voltage for the same voltage requirements. As a result, the substation area or size can increase in the same ratio

cost per mile, there is a very obvious possibility of justifying 69 kv. The greater the distance from this substation to the source of 69-kv supply, the greater become the 69-kv benefits.

Subtransmission Area Coverage a Cost Obstacle to 69 Kv

If, however, 16 small unit substations had been chosen instead of one large substation, we would have wound up with four 12-kv substations which presents a different problem on the 34.5-kv side as shown in Figure 10. Here the benefits of 69 kv over 34.5 kv are reduced because the 69 kv has to cover more area or go to more points and pick up smaller loads. The result is to give loads on 69-kv lines between substations that are not great enough to show a substantial saving per mile at 1.5 times the cost per mile. In that case there must be a substantial distance coverage benefit from the load area under consideration to the source in order to justify 69 kv over 34.5 kv. This illustration ignores the question of the most economical substation size. This will be treated later.

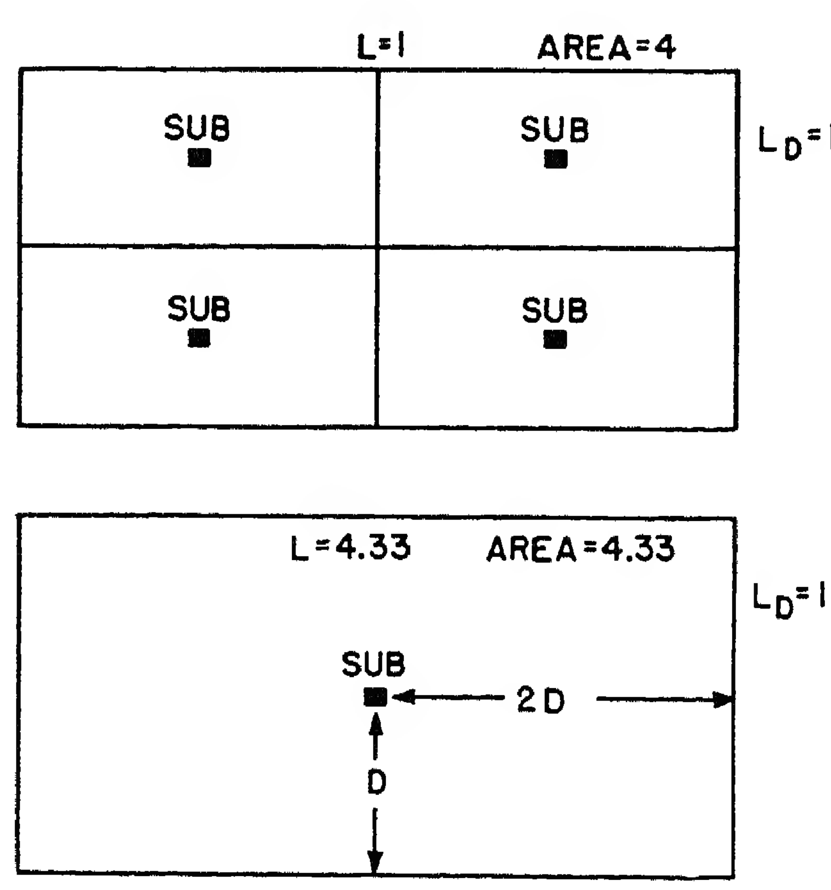
Twelve-Kv Area Coverage Costs More Than 4 Kv

So far benefits have been considered in the terms of load carrying ability. The relative costs should be examined now to see just where it is that money is saved. Loss evaluation is promptly disposed of because it is the same per-cent loss in either case. The simplest approach is to start at the load end.

It is obvious from Figure 2 that transformers with arresters and cutouts for 12-kv circuits cost substantially more than 4 kv. This greater cost diminishes as the transformer size increases. So substantial savings must be found elsewhere to make up for this higher cost of 12-kv transformer installations.

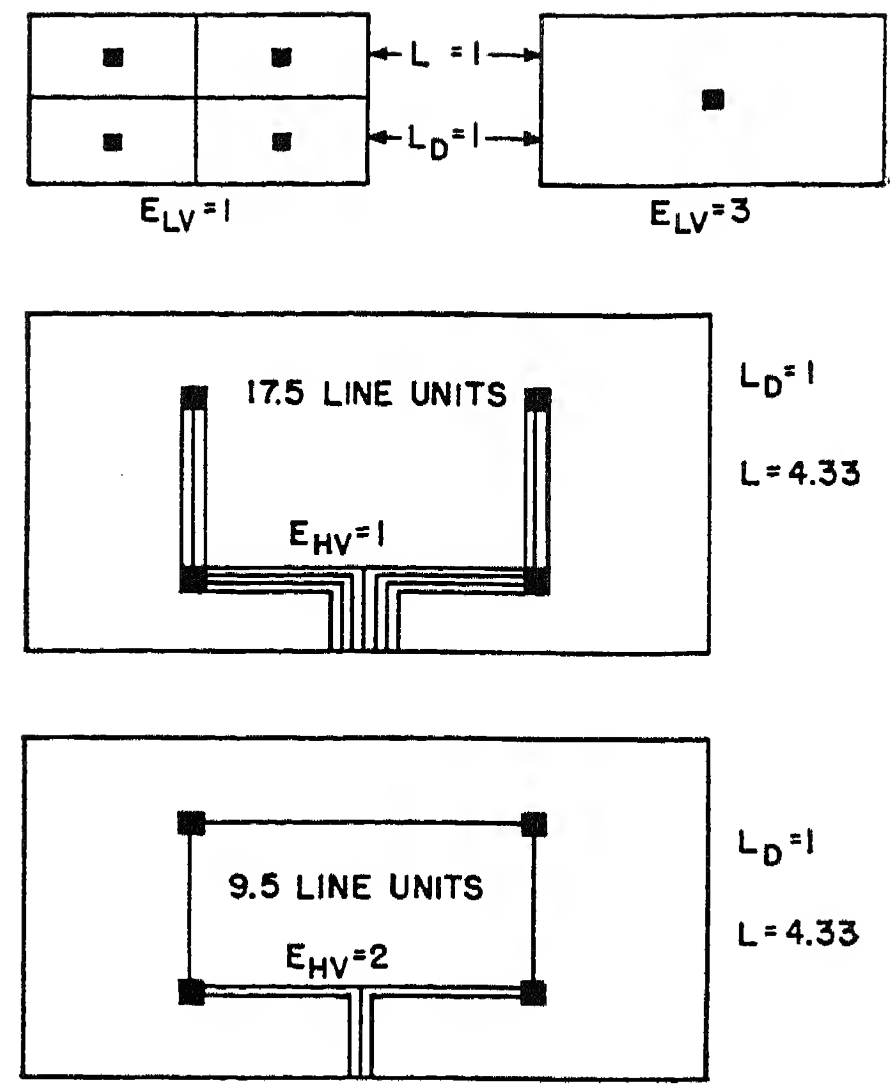
The next step is to look at line costs for the special case of distributed loads. It is at once obvious that just so long as we are limited to distance coverage in one dimension and using the voltage square factor of three times the load times three times the distance that the load divided by distance or vice versa is a constant and equal to unity so that the circuit miles per kilovolt-ampere do not change and no benefit is derived from line costs even if the 12-kv line costs the same per mile as a 4-kv line. Therefore, that portion of the circuit between the first transformer and the last transformer costs more per kilovolt-ampere for 12-kv distribution than for 4-kv distribution for both the line and the transformers.

In the case of changing both dimensions the same result is obtained because increasing the length of lateral by 2.08 also increases the length of main and hence the number of laterals by 2.08. Therefore, we come out with 4.33 times the load and 4.33 times the mileage of line. Thus it is clear that higher voltage cannot possibly save money in the load area itself. Load area coverage definitely increases on a cost per kilovolt-ampere basis as the voltage increases. All savings, therefore, must come from the feeders and substations supplying the load area. This turns out to be substantially a plain distance coverage benefit so well known in transmission.



TRANSMISSION SUPPLY AT DOUBLE VOLTAGE COSTING 1.5 TIMES PER MILE WOULD CARRY 4 TIMES THE LOAD PER CIRCUIT AT $\frac{1.5}{4}$=.375 TIMES THE COST.

Figure 9. One big substation covering the same area as four small ones greatly reduces the mileage of transmission circuit and thus tends to favor a higher economic choice of voltage level



1.5 X $\frac{9.5}{17.5}$ =.81 TIMES THE COST. FOR E_{HV}=2

Figure 10. One large substation will feed an area fed by four smaller ones. If in turn these larger substations could be grouped in a large area to be fed at $E_{HV}=1$, a saving in line cost could result from double the voltage but not nearly as great as Figure 9. This and Figure 9 show how increasing the low-voltage level tends to favor a higher high-voltage level

Twelve-Kv Savings Occur in Substation and Its Feeders

So let us look at the feeder from the feed point, or first transformer in the feeder load area, to the substation. Here is the most favorable result from 12 kv. In one case one feeder is replacing three 4-kv feeders for distance coverage of distributed loads as in Figure 4 and in the other case one feeder replaces 4.33 4-kv feeders as in Figure 7. These savings for 12-kv feeders are also greater for the lighter densities and less for higher densities. However, as densities become increasingly greater, a point is reached where line congestion becomes a factor and 12 kv will make it possible for substations to be four times larger before the same congestion is reached in terms of the number of feeders.

Next is the substation itself. Figure 11 shows that if there is enough load to put 5,000 kva on a 12-kv feeder, the substation may cost about \$2.00 per kilovolt-ampere less. So up to this point a sub-

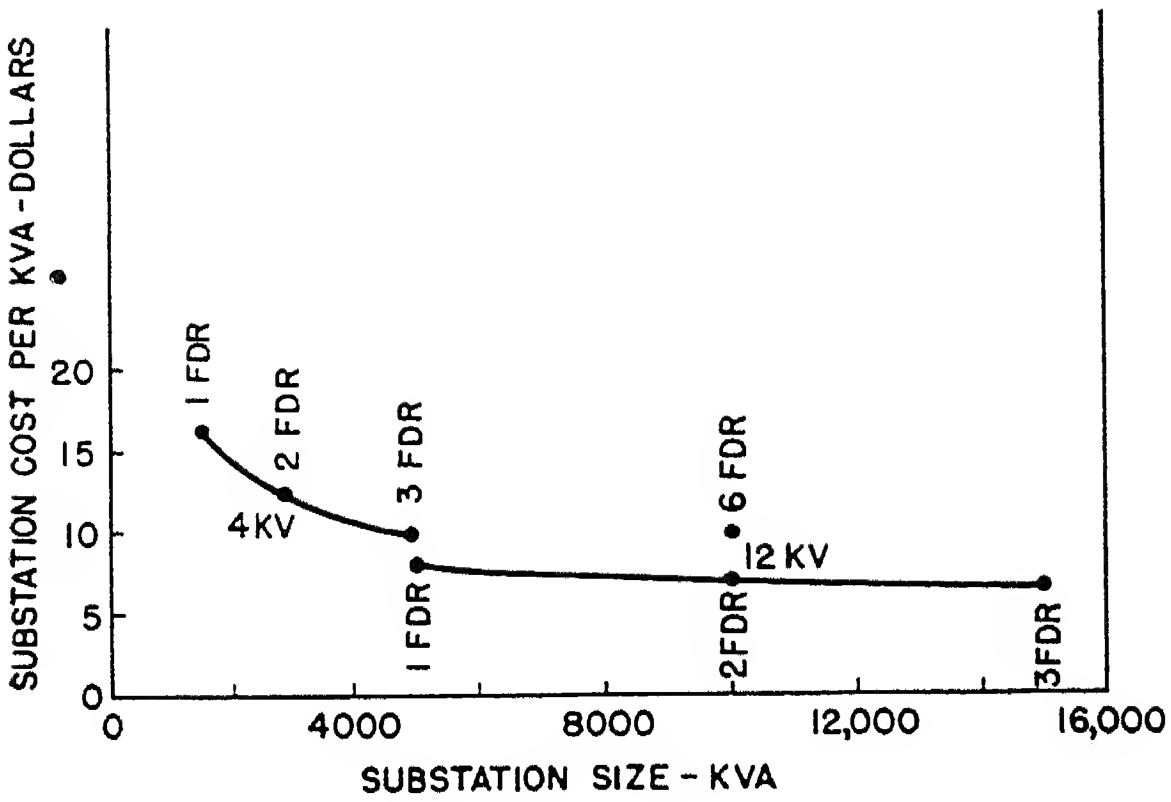


Figure 11. Cost of 33-kv substation capacity. Absolute substation costs depend to a large extent on its pattern of connections. A simple pattern would diminish in cost with load and voltage increase as illustrated. A more elaborate pattern would tend to give a steeper curve or more change with load increase

stantial saving in line mileage would be needed to make up for the distribution transformer investment difference of $4.00 to $6.00 per kilovolt-ampere. This latter applies to the total transformer kilovolt-amperes and may exceed the feeder kilovolt-ampere as much as 1.5 so the cost increases to $6.00 to $9.00 per kilovolt-ampere. Figure 12 will help to show how much this might amount to in the case where load is proportional to distance. It has been seen that the only place available for saving money in such cases is the feeder circuit between the feed point and the substation. The particular cost and loss evaluation shows that this can be enough to counterbalance effectively the higher cost of 12-kv distribution transformers and have the substation saving as a good margin. Figure 13 shows less advantage when the feeder area size and load is increased 4.33 times and the substations are located centrally as illustrated. It has been seen from Figures 9 and 10 that the additional benefit of larger substation size attending 12 kv may bring savings on the subtransmission supply by creating a favorable situation for a higher subtransmission voltage. As pointed out, load and voltage must travel in pairs if there is to be a saving. Increased voltage is of no benefit without increased load to make it economical. They are the "Gold Dust" twins.

Twelve-Kv Greatest Benefit Occurs When Initiated at Light Load Density

Cost evaluations made at a fixed density are easily modified by merely changing the dimensions of the pattern and thus obtaining some idea how cost changes when the density is changed. Let us go back to Figure 12 and double the dimensions (in this case length only) and thereby double the area resulting in one-half the density if the same total load on the circuit is kept. But it is not possible to keep within the prescribed voltage drop if the dimensions are doubled so the loading will have to be limited to one-half. In that case the density becomes one-quarter whether it is expressed as kilovolt-amperes per 1,000 feet or kilovolt-ampere per square mile. Instead of 2,000 kilovolt-amperes per circuit we have 1,000 kilovolt-amperes per circuit at 1,000 kilovolt-amperes per square mile. A 12-kv circuit thus becomes limited to 3,000 kilovolt-amperes.

Since the number of the circuits does not change, substation switching cost goes up inversely with the load and consequently saving in substation because the transformer cost per kilovolt-ampere is substantially the same for either 4 or 12 kv. This concept is true only within the range of kilovolt-ampere ratings that do not produce much difference in interrupting capacity required. For convenience $4.00 per kilovolt-ampere saving in substation at the lower density will be assumed.

The circuit length between the feed point and substation doubles when the distances are doubled. The total cost in dollars doubles and the difference or savings in dollars also doubles. But the load is one-half so the saving in cost per kilovolt-ampere saving of feeder circuit length becomes four times that shown in Figure 12. The losses in per-cent kilowatts do not change and so the evaluation per kilovolt-ampere remains the same as before because we have one-half the kilowatt loss at one-half the load.

Economic Feeder or Substation Size Increases Slowly with $\sqrt[3]{\text{Load Density}}$

The same remarks apply to Figure 13. The lengths of the feeders and their difference or savings change with the dimension. The area changes as the square of the change in dimensions. The load must change inversely with the dimensions to give the same voltage drop in this case too because the number of branches in the load area are kept constant. So the result is a load area four times as big at one-half the load resulting in one-eighth the density. The load that can be carried and kept within the same voltage drop between the first transformer then varies as the cube root of the ratio of the load density when all dimensions are changed in the same proportion. So if there is a 1,000-kva circuit supplying or covering 2 square miles at 500 kva per square mile, there will be a 2,000-kva circuit covering a quarter or 1/2 square mile at eight times the density or 4,000 kva per square mile. Therefore, the load that can be carried increases very slowly with density.

Number of Feeders or Substations Increases Faster with $\sqrt[3]{\text{Load Density}^2}$

On the other hand, if the density of the square mile increases uniformly to eight times there would then be four circuits each of 2,000 kva each or the number of circuits increases as the cube root of the square of the ratio of the load density. So from this it is seen that the slow rate of increase in feeder size necessitates a faster increase in the number of circuits. Since

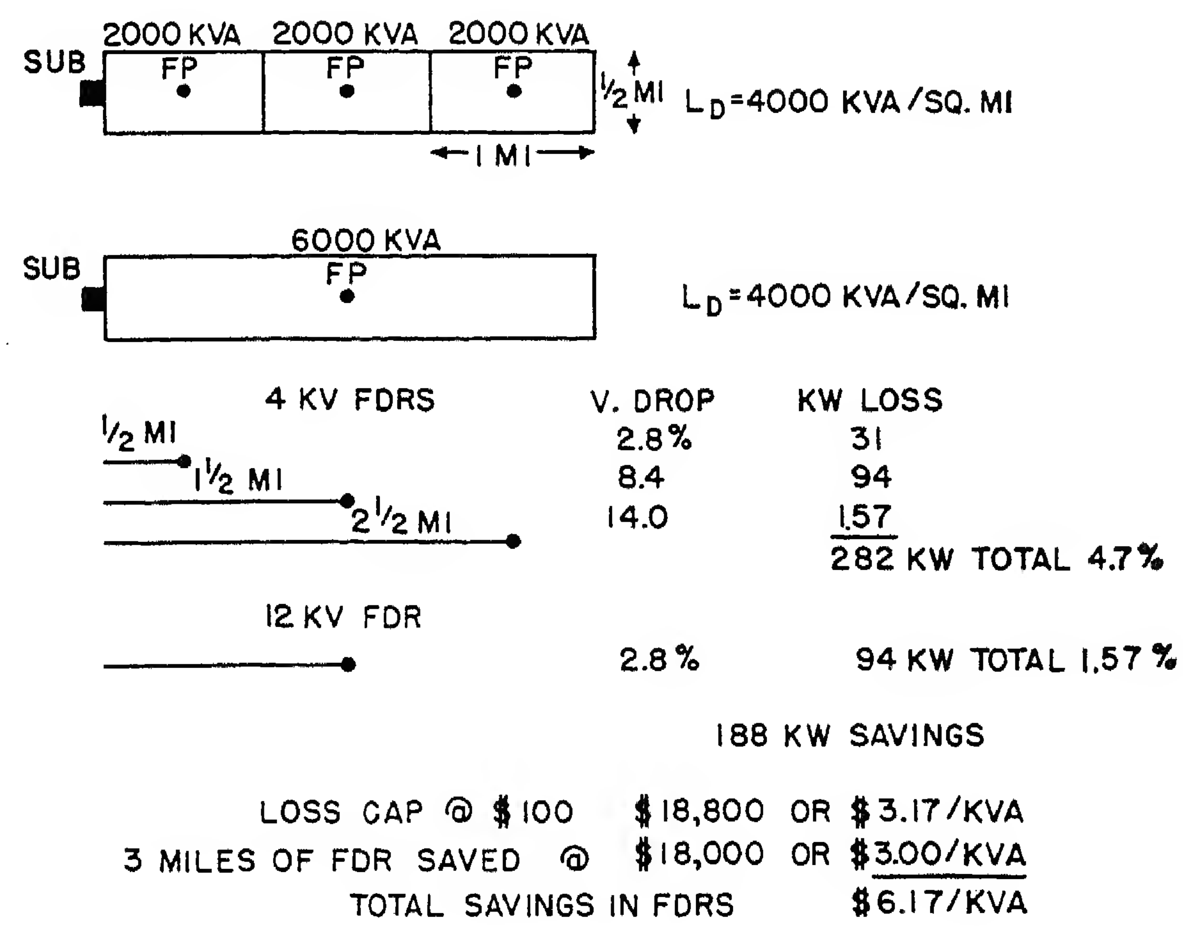


Figure 12. For uniform load distribution and contiguous areas the cost per kilo-volt-ampere of lines and losses in per cent between FP and the distribution transformers is the same for either voltage. The cost per kilovolt-ampere of feeders between FP and the substation is less for the higher voltage. To this is to be added saving in substations and to be subtracted is the voltage charge for transformers and accessories

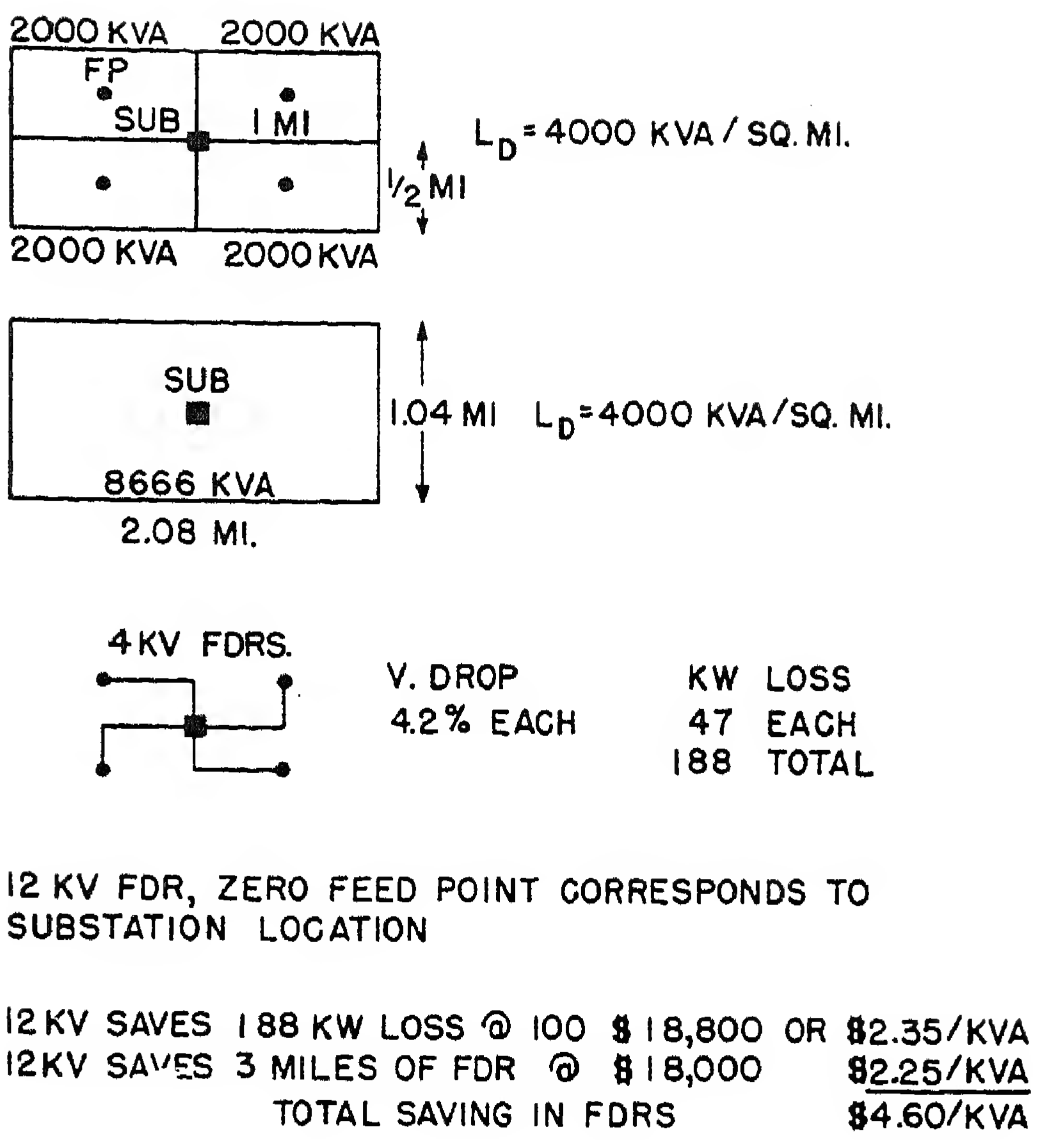
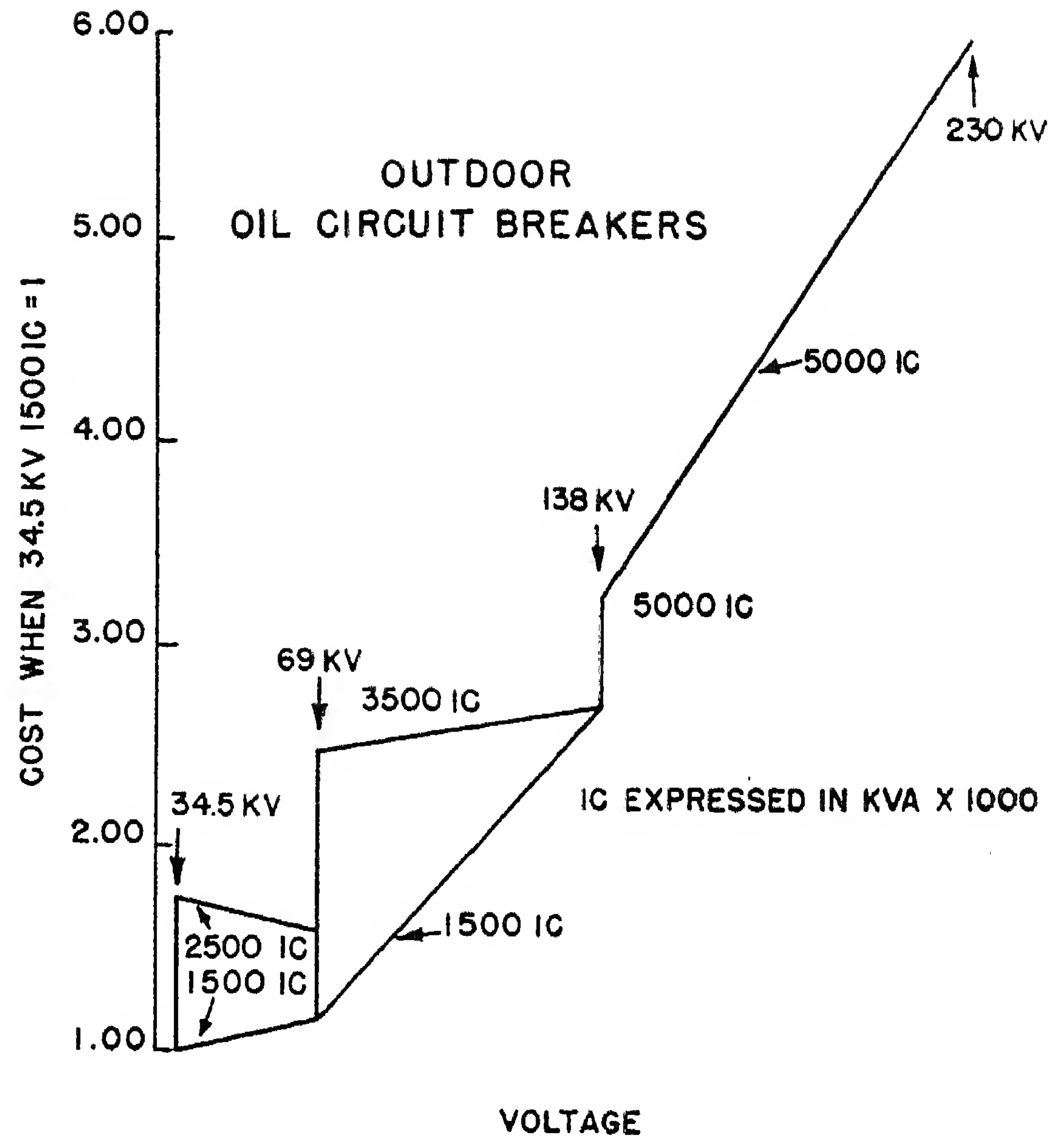


Figure 13 (left). The central location of the substation in a rectangular area saves less than Figure 12 because the distance between substation and FP is less

Figure 15 (right). Circuit breakers likewise have a sharp rise in voltage charge. There is some compensation on large capacity systems by the increased interrupting capacity that tends to accompany the higher voltage



these factors apply to either voltage level it becomes evident that the 4-kv system will run into pole line congestion much sooner than the 12-kv system. Thus we see that the earlier a higher voltage level is introduced in the area, the more there is to be gained. At 500 kva per square mile much more is gained in cost than at 4,000 kva per square mile. If 4,000 kva per square mile should be the point of undesirable pole congestion at 4 kv with 12 kv the corresponding point of congestion would not be reached until 4,000×4.33 or 17,300 kva per square mile or a density ratio of 34.6-to-1.

Economic Substation Size Influenced by Ratio High-Voltage to Low-Voltage Line per Cost Mile

Thus far a definite ratio has been assumed in substation size based entirely on the choice of 4 kv or 12 kv. It is seen that the larger substation for 12 kv tends to require a higher subtransmission voltage level to go along with it like going from 34.5 kv to 69 kv. It should be pointed out now that once a voltage level is chosen for the distribution system, the economical substation size to supply its circuits is in a great measure determined by the ratio of the cost per mile of the subtransmission to feed it and the cost per mile of the distribution feeders to connect between the individual feeder areas and the substation.

Since, therefore, 12-kv overhead lines do not cost much more per mile, say 1.1, than 4-kv lines and since 69-kv lines might cost 1.5 times the cost of 34.5-kv lines per mile, a new situation is created requiring a review of the economical substation size for 69-kv to 12-kv substations which may be larger than the 4.33 ratio previously given based on the consideration of 4 kv and 12 kv alone. Whatever the ratio of 34.5- to 4-kv line cost per mile is the 69-kv to 12-kv cost per mile ratio will be substantially greater. This greater ratio of 69 to 12 kv will have the effect of increasing the 12-kv substation size to a ratio greater than 4.33. What happens is that the larger substation puts back some of the money saved in 12-kv distribution feeders in order to save a corresponding greater amount in 69 kv so that the overall saving will become greater.

Twelve-Kv Substation Economic Size May Be Greater than 4.33

Thus if we started with a 5,000 kva 4-kv substation we would have a 21,650-kva 12-kv substation considering only voltage drop in the 12-kv circuits. If the load area were large enough and contiguous enough the new 69- to 12-kv substation may be still more economical at a higher value like 25,000 kva or even 30,000 kva because of the increased ratio of high-voltage to low-voltage line costs per mile. To continue the process onwards and upwards, if we had the area and load to go with it, it might be found that the previous 34.5 kv was being in turn fed from 115 kv or 138 kv. So substituting 69 kv for 34 kv tends to involve a shift from 115-kv/34-kv/4-kv system to a 161-kv/69-kv/12-kv system or possibly a 230-kv/69-kv/12-kv system for maximum economy. This is not meant to imply that such changeovers could actually be undertaken but only the cost relations tend to do that and savings may not be great enough in magnitude to absorb the costs of changeover.

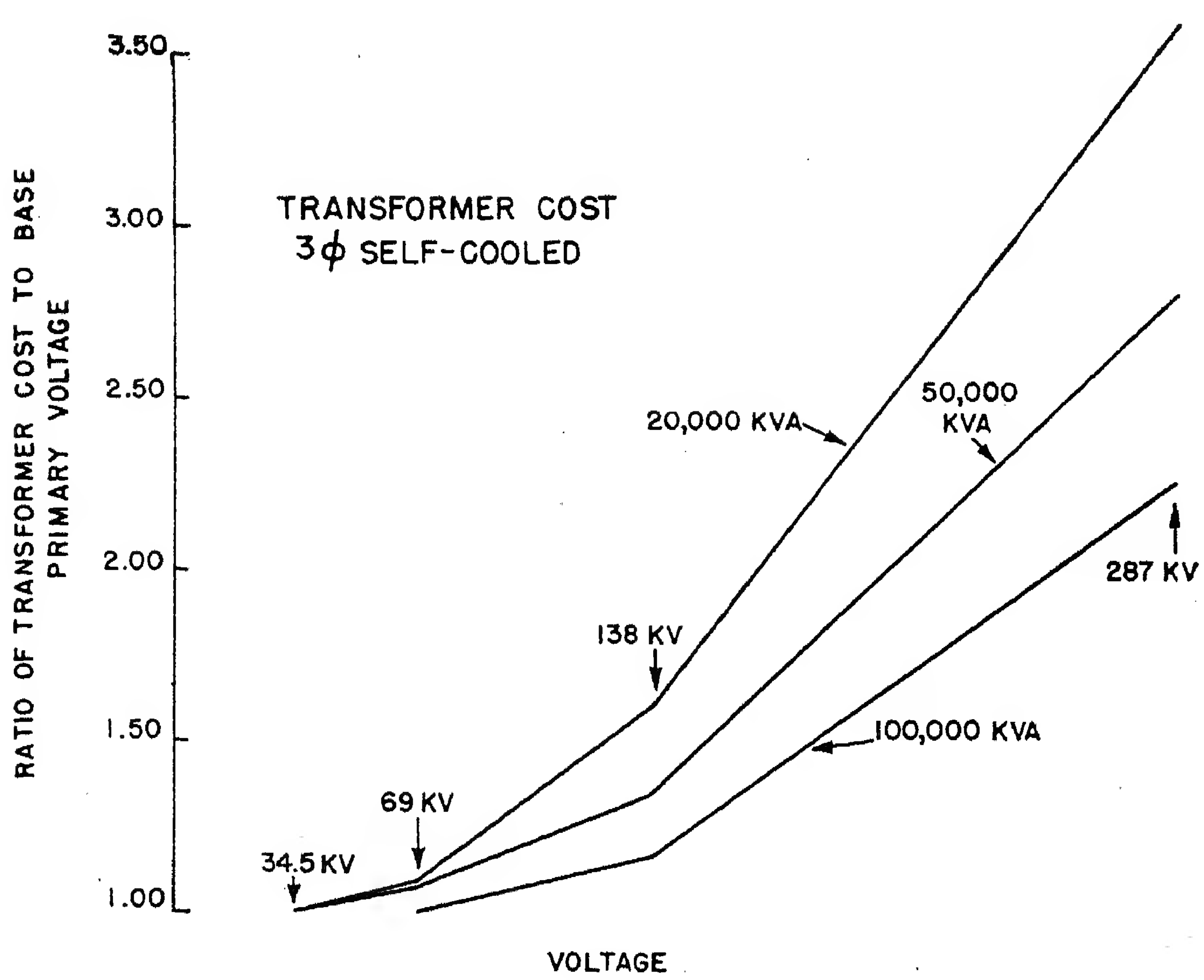


Figure 14. Voltages above 69 kv incur a sharp rise in voltage charge for a given size of transformer. This must be offset by a much larger kilovolt - amperage for the higher voltages plus savings in line costs

Subtransmission Voltages Above 69 Kv Have Stiff Voltage Charge Obstacles

However, above 69 kv there are some pretty stiff cost obstacles that will require large amounts of load to bring them within the economic range. These obstacles are the voltage charges for the major apparatus like transformers and circuit breakers. Figure 14 shows how rapidly the transformer cost goes up with voltages above 69 kv and Figure 15 illustrates the circuit-breaker picture. These voltage charges affect the benefits of the voltage square factor in such a way as to restrict the number of transformers and circuit breakers that can be connected to a circuit. The final result is to justify very large substations to transform from these higher levels to lower levels. The concept of distance coverage predominates in their application rather than the concept of area coverage.

Figure 15 is of particular interest because it shows the higher interrupting kilovolt-amperage available and the small spread in its cost at and above 138 kv. This is an additional benefit for these higher voltages. As systems grow and reach a short-circuit duty bottleneck at the lower voltage the higher voltage gives a way out by the much greater interrupting capacities available. Thus the higher-voltage charge brings a benefit that enables the lower-voltage system to be subdivided and supplied from a higher-voltage level which bears the burden of future growth.

Field Conditions Have Been Chief Obstacle in Way of Acceptance of 12-Kv Economy

The foregoing discussion has been largely theoretical ignoring field conditions that tend to favor one voltage class more than another. The most common and obvious field condition is that of trees. The custom of using weatherproof wire at 4 kv is maintained largely on the basis of the short-time insulating value of weatherproof covering. The value of this covering occurs under the condition of heavy winds blowing limbs into the wires and pressing them together and the presence of foreign objects across wires. Experience has shown that this covering does prevent arcing and consequent circuit-breaker operations although it is not designed for permanent contact between conductors. Twelve-kv bare wire would require greater conductor spacing and a greater amount of tree trimming to prevent limbs pushing the conductors together for weatherproof covering is not used at 12 kv. Four kv is commonly worked hot with rubber goods whereas 12 kv is ordinarily worked with hot line tools.

These two factors plus the inherently greater area coverage with load and longer time per outage have been mainly responsible for the slow acceptance of 12-kv class voltage for urban domestic areas. The economy of the 12-kv voltage is not substantial enough, in the opinion of many operators, for them to accept these disadvantages. However, in recent years there has been a very substantial increase in the use of this voltage class in suburban residential areas. In rural and industrial areas the 12-kv class, including 13.8 kv, is widely used.

On the other hand, increasing load densities often produces circuit congestion and real estate difficulties for 4-kv substations. The ratio of 4.33-to-1 gives 12 kv a big advantage from this viewpoint and from the viewpoint of subtransmission lines of 34.5 or 69 kv to supply the substation.

The Short-Circuit Duty on Fuse Cutouts Is Substantially Increased With 12 Kv

The same short-circuit current at 12 kv means three times the energy or kilovolt-amperes to be interrupted. In Figure 3 it is seen that the short-circuit current is the same value in amperes at the end of the circuit. The current increases in the same proportion on a percentage basis as one goes closer to the substation. That is, at the half-way point the amperes are still equal. At an equal distance in feet the 12 kv has three times the amperes.

The area coverage picture, however, is somewhat different. The short-circuit amperes are 44 per cent greater at the end and at other points on a percentage basis. This means 4.33 times the kilovolt-amperes. This is for the worst condition of a sustained voltage at the substation bus which can apply only to very large stations when the short circuits get near to but not at the substation bus.

Figure 7 shows that the substation short-circuit kilovolt-amperage is up only 8 per cent because we took 12-kv benefits in the feeder size only and thereby only increased the substation size in the ratio of 4.33-to-4. Ignoring this 8 per cent gives nine times the ohms reactance at 12 kv and so the same short-circuit kilovolt-amperes or one-third the current at the 4-kv substation. By looking at Figure 8 it is seen that if we take 12-kv benefits by increasing the area to keep the same number of feeders the short-circuit kilovolt-amperes is increased to 4.33 times for circuit breakers. This means that if the 4-kv substation reactance in ohms is taken as equal to Z_T then the 12-kv substation for Figure 7 will be 9 Z_T and for Figure 8 Z_T will become (9)/(4.33) Z_T. Similarly, if the feeder length for any particular area is taken as equal to Z_F at 4 kv, it will be 2.08 Z_F at 12 kv because of the distance factor D.

This applies to Figure 8 also so it is possible to define the short-circuit current at corresponding feed points as $I_{SC}=3E/[2.08(Z_T+Z_F)]$ since $9/4.33=2.08$. Since $3/2.08=1.44$, the individual areas will have a short-circuit current 1.44 times as great or a short-circuit kilovolt-ampere of $1.44\times3=4.33$ times as great. The general equation is

$$I_{SC}=\frac{\left(\frac{E_2}{E_1}\right)E_1}{\sqrt[3]{\left(\frac{E_2}{E_1}\right)^2}(Z_T+Z_F)}=\frac{E_2}{\sqrt[3]{\left(\frac{E_2}{E_1}\right)^2}(Z_T+Z_F)}$$

Z_T being the ohms reactance of the original voltage and Z_F the ohms reactance at the original voltage and distance.

This equation is also good for any point on the lateral Z_L within the load area beyond the feed point provided there is no change in the geometry of the pattern. Nonuniformity in field conditions precludes such a simple relationship in practice so that the percentage of the cutouts receiving different degrees of duty can vary substantially up or down. However, it is evident that higher voltages require higher interrupting capacities and higher interrupting capacities increase the cost of transformer installations.

Full Benefits of 12 Kv Require Same Use of Regulating Devices or Methods

The foregoing analysis assumes that there is no change in the means of regulating the voltage. This must be true in order to get full benefits for the higher voltage. If the 4 kv has feeder and supplementary regulators then the 12 kv likewise should have feeder and supplementary regulators for full benefits. Comparison of particular situations with regulators in 4 kv and none in 12 kv may show an advantage for 12 kv, but such cases do not fully utilize the capabilities of 12 kv. So the interpretation must not be made that feeder and branch regulators are not economically useful at 12 kv.

Feeder regulators and branch regulators are useful at any voltage level to extend the economic range of that voltage. The economic principles determining regulator applications are the same for any voltage level.

Twelve Kv Means Greater Outage or Less Savings

The greater area coverage of 4.33-to-1 as already noted means there is 4.33 times the mileage of line for one 12 kv as for one 4-kv circuit. With the same trouble rate per mile of line one 12-kv circuit will have the same number of outages as 4.33 circuits operating at 4 kv in the same area would have. Each time an outage occurs on the 4-kv circuit unit load would be interrupted and 4.33 circuits would interrupt 4.33 load units. The corresponding 12-kv circuit would interrupt 4.33 units of load for each outage. Since the rate per mile is constant, there would be 4.33 load units interrupted 4.33 times. This means that each customer would observe 4.33 times the number of interruptions.

If the time to locate permanent faults is proportional to the mileage, each customer would experience 4.33 times this interval plus the time to repair. The latter, of course, may be constant. The effect of this outage time and number of customers can be reduced by having more sectionalizing in the 12-kv circuit than in the 4-kv circuit. Of course, the 12-kv circuit could be restricted in its extent and carry less load.

Any method used to reduce the disadvantage of more customers out a longer time means some 12-kv savings have to be sacrificed. Fortunately automatic reclosing devices reduce the seriousness of this disadvantage to those faults classified as permanent. A low percentage of permanent faults is, therefore, essential to higher economy with higher voltage without a substantial sacrifice in outages.

High load densities serve to reduce the seriousness of this factor because the mileage per circuit is reduced accordingly and the number of faults per circuit is thereby reduced and the consequent time to locate a permanent fault is reduced. Substations being larger and fewer enable 12 kv to have 4.33 less faults in number but 4.33 times the load for each so each customer notices no change as far as substation outages are concerned.

The evaluation of outage versus savings is a matter of judgment. Men differ in their judgment with the same set of facts; therefore some will think the savings worth the outage disadvantage and some will not. Since it is their money their judgment is the one that counts.

Nonuniformity of Field Conditions Tends to Favor 12 Kv Over 4 Kv

Nonuniformity of field conditions serves to upset these general observations. The most common condition of nonuniformity is the existence of substantial space or gaps between load areas. In effect, these serve to reduce the average load density which has been noted as more favorable to the higher voltage. Another common condition is the large individual loads that have a wide range in demands and in distribution. This effect is reduced substantially by the higher voltage by increasing this range in demands and in distribution by its voltage square factor before they will have a corresponding effect on 12 kv that they had on 4 kv. Another way of looking at it is to think of these individual loads with a certain demand and a certain spacing between them. The voltage square factor divides their influence by nine and to that extent serves to make the loads appear to be more uniform.

Almost any given set of nonuniform conditions at 4 kv turns out to be less influencing at 12 kv. Included in this observation should be those fluctuating loads which tend to cause disturbing lamp flicker. A voltage square factor of 9 applies to them as we saw in Figure 1. For 12 kv they can be nine times as big for the same distance or three times as big for three times the distance as far as the effect of line impedance is concerned.

The Present System Is a Deterrent

The present system is a severe handicap to going to a higher voltage level. The first essential is a load growth large enough and fast enough to subordinate, in a reasonable time, the lower system voltage. The second essential is a planned conversion that takes a section at a time so that two things are accomplished. First, purchase a minimum of new apparatus for the lower voltage. This is accomplished by using apparatus of converted sections to furnish a stock for load growth in sections not converted. Second, the rate of conversion is controlled so that the present system is maintained the rest of its useful life consistent with service standards and maintenance requirements.

The fastest growing areas should be selected for conversion first to avoid prolonging life of the lower voltage system. Some sections may be impractical or undesirable to convert. They can be maintained and supplied from small unit substations supplied in turn from the new higher voltage. Conversion itself is not the objective. The objective is to get maximum benefits practical from the higher voltage and only such conversion that will attain this result is carried out step by step.

Conclusion

Given enough load and enough contiguous area a good case can be made for 12 kv over 4 kv. The magnitude of this saving has not been stressed in this paper. Such cost values that have been used were used only with the intention of indicating possible magnitudes relatively and to illustrate a method of comparison.

The higher voltage benefits are contingent on the same voltage drop being allowed and same regulating methods for maximum benefits.

Savings come principally in feeders and substations and are maximum at lighter densities.

At any higher-voltage level the tendency is to more and longer outages from the customer viewpoint with increased short-circuit duties on interrupting devices.

The 12-kv level tends to justify a higher subtransmission supply than 34.5 kv. Above 69 kv the voltage charge for apparatus puts a severe check on this tendency. Other system requirements may preclude conversions at these higher levels as well as the relatively small difference in cost.

Four-kv systems fed from 12-kv systems give a clear-cut saving in substation costs by eliminating one voltage level.

Obviously benefits from smaller copper could be substantial where the area does not have enough load to fully utilize both large copper and higher voltage.

Sectionalizing means could be used at either voltage level but in view of greater area coverage may be more worth while at 12 kv than 4 kv and thus give an acceptable continuity performance at the higher voltage.

No Discussion

Ice-Melting and Prevention Practices on Transmission Lines

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ICE storms generally occur each year in the 11,000 square miles of territory served by Public Service Company of Northern Illinois. Located in the territory are approximately 3,200 circuit miles of 33-, 66- and 132-kv overhead transmission lines and about 21,000 miles of distribution circuits. Icing conditions affecting these lines have been recorded as early as November 15 and as late as April 15, a 5-month period. There appears to be no regular cycle for the occurrence of the ice storms nor is there any pattern as to their extent or severity.

Information on the approach of a storm is received from various sources including outlying substations, airway radio stations, and the United States Weather Bureau's special forecasts to the company concerning icing conditions. Some of the company's personnel have become quite experienced in weather forecasting and in keeping the power supervisors informed on the location and progress of the ice storm.

Experience has shown that prompt action is of the utmost importance in ice-prevention and ice-melting practices in order to take advantage of the higher ambient temperatures that are usually prevalent during the formative period. A drop in temperature is quite likely to occur at the end of this period which may make melting difficult or impossible. Also heavy snow sometimes follows the icing period which may make transportation difficult for switching or maintenance crews. The emphasis is, therefore, on speed of melting, using high-current values over comparatively short-time periods.

In order to expedite the ice-melting operation, gang-operated air-break switches have been installed at strategic locations in lines to eliminate the necessity of installing short-circuiting jumpers. At some locations, short-circuiting load interrupter switches have been installed to apply and remove ice melt currents ranging in magnitudes up to 600 amperes. These switches eliminate the necessity of taking these lines out of service for application and removal of short-circuiting equipment, thus reducing interruption time to customers served from the line. Special disconnects and ice-melting busses have been installed at substations to afford flexibility as to source and potential. Operating routines for the various melting methods with all pertinent data are prepared in advance to eliminate time delays that might occur if it were necessary to work up this information during an icing period. Test melting operations are performed on lines prior to the preparation of final routines to detect any weaknesses in the ice-melting circuit such as poor connections, joints, and so forth. and to verify calculated current values. Correction measures can then be taken under better conditions than those which prevail during an ice storm.

As the magnitude of power requirements is high, especially when melting with 33-kv potential, it is, of course, essential that melting centers be backed up with a firm source such as the 66- and 132-kv systems, and that sufficient transformer capacity to carry both the melting current and the normal load current simultaneously be provided.

Ice-Prevention Practices

Load-Shift Method

Ice-prevention methods are practiced on 33-, 66- and 132-kv lines in the company territory as soon as icing possibilities are reported. The simplest method practiced to prevent ice formation is that in which additional system loading is placed on transmission lines in the ice-storm area by removing from service lines outside of the area, or removing from service lines in the storm area from which ice can be more readily melted later. Sufficient current is thus placed on lines to prevent ice formation. This current is considerably less than that which otherwise would be required to melt ice.

Circulating-Current Method

Another method of increasing the current on lines for ice prevention is causing circulating current to flow in transmission line loops by maintaining a voltage differential across the loop at the substation terminals. Typical examples of the use of this method is at Bellwood Substation where two 60,000-kva 66/33-12-kv transformer banks are switched so that each transformer bank supplies a separate 33-kv bus. The ends of a 6-mile transmission loop are connected to the separate busses. By adjusting the voltage regulator on each transformer, a voltage differential can be obtained to cause approximately 200 amperes of circulating current to flow through the loop in addition to the normal load current. Similarly, approximately 200 amperes can be caused to flow through another transmission loop between Bellwood and Forest Park Substations.

The 66- and 132-kv lines must receive first attention during an ice storm in order to keep the major substations and supply points in condition to supply the lower voltage transmission lines and furnish adequate capacity for melting from these points. Prevention practices are, therefore, first placed into effect on these lines upon approach of an ice storm.

Unfortunately, the ice-prevention methods do not give 100 per cent coverage on all transmission lines and ice accumulates on the lines not loaded for prevention. It, therefore, becomes necessary to remove it as promptly as possible before winds acting on the icy conductor causes galloping resulting in conductors contacting and burning down or before line failures occur from weight of ice. The 66- and 132-kv system is again given first consideration in ice-melting practices for the reasons mentioned previously.

Ice-Melting Practices

Short-Circuit Method

The short-circuit method of melting ice from transmission lines consists essentially of short-circuiting or grounding the line at one end and applying a potential of sufficient magnitude at the other end as to cause ice-melting current to flow over the line. Curve sheets showing relation between current magnitude and melting time for various ambient temperatures and wind velocities have been prepared for various size conductors. These are based on 1/4-inch ice accumulation with wind direction at right angle to the conductor, and are used as at time guide in this and other methods of ice melting. Figure 1 shows the time-current relation for 3/0 copper conductor. For example, this curve sheet shows that if only 400-

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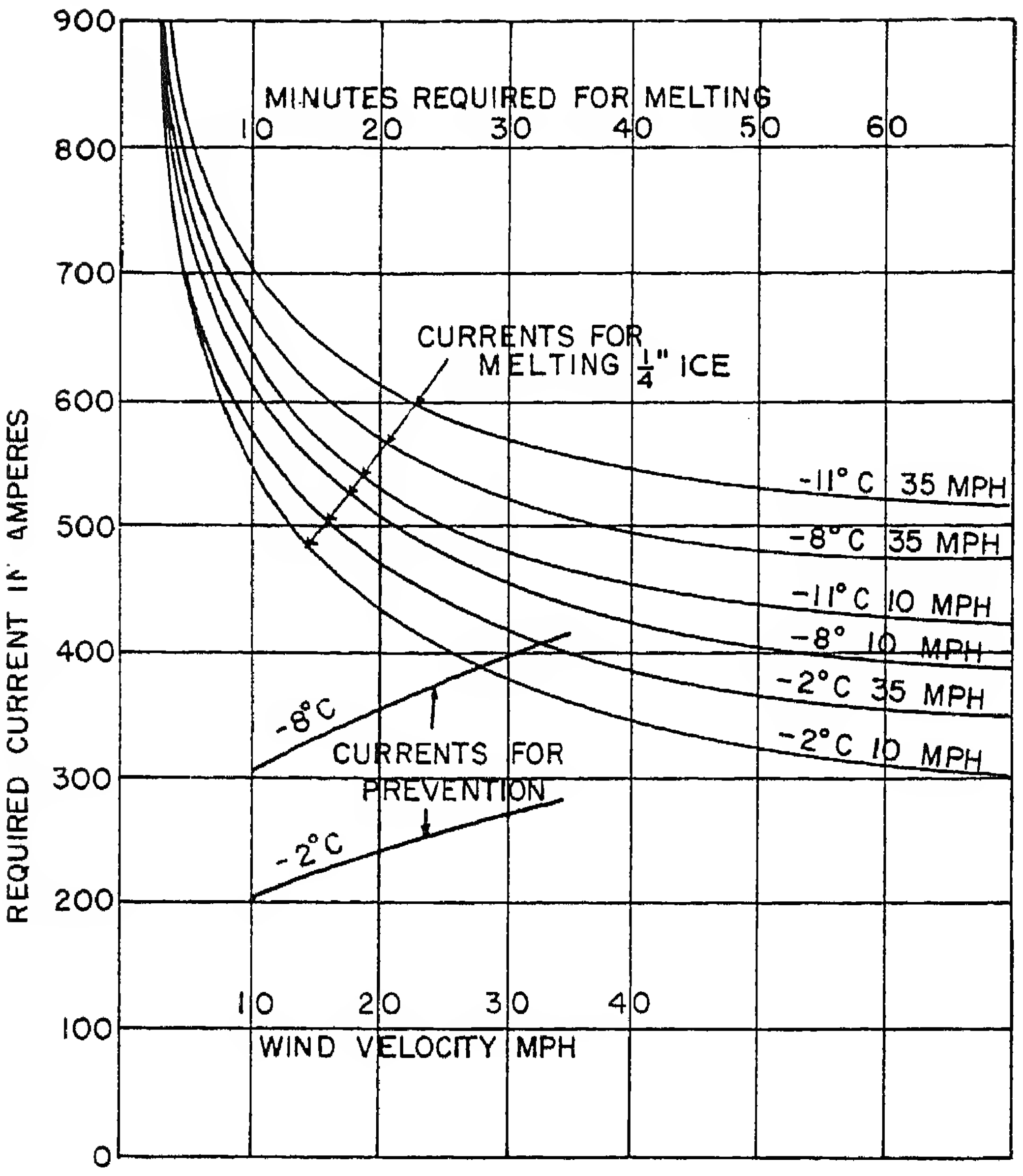
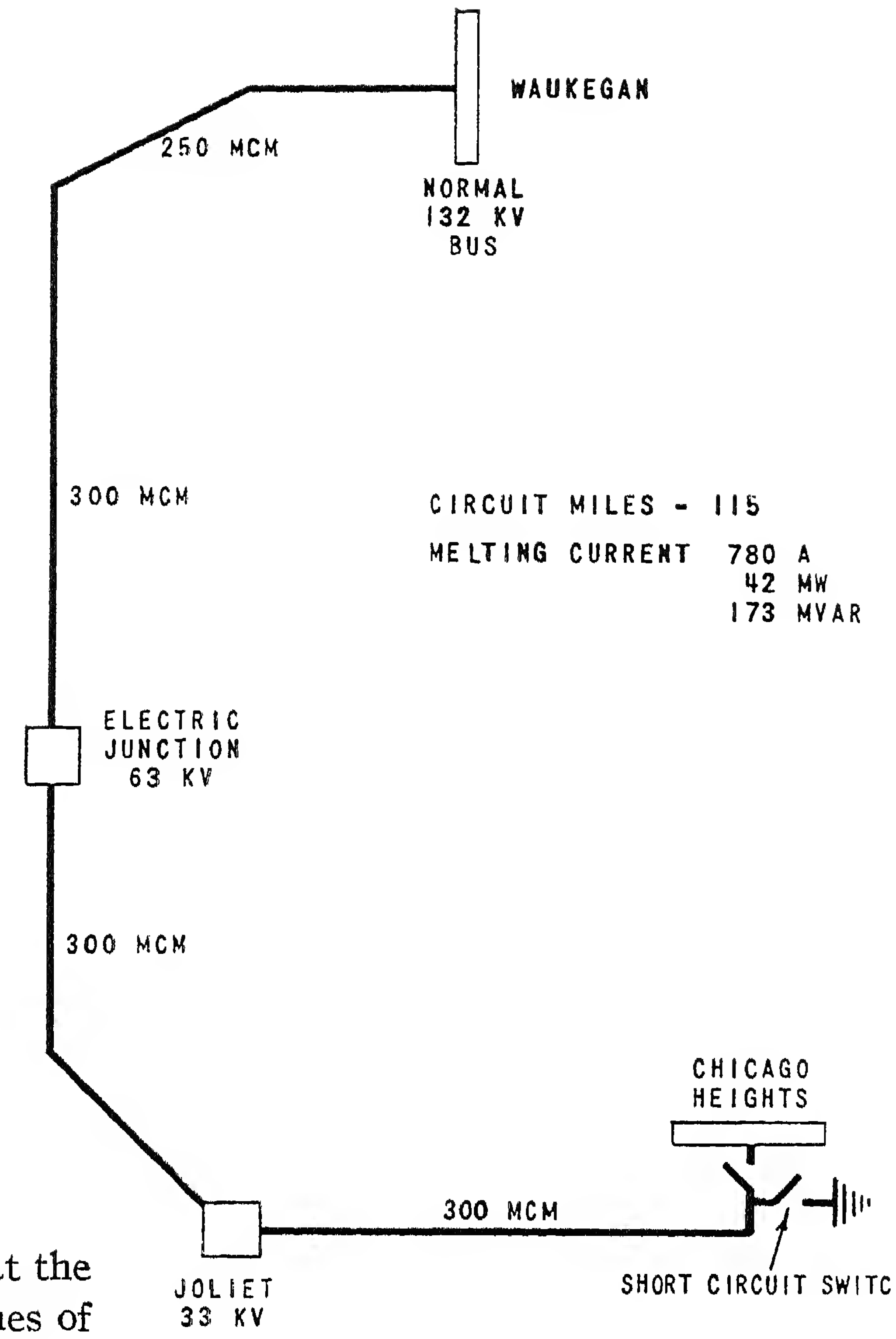


Figure 1 (left). Ice-melting and prevention curves for size 3/0 hard drawn bare copper conductors

Figure 2 (right). Short-circuit method of melting ice on 132-kv transmission lines at 132 kv

Melting current, amperes—780
Melting time, 28 degrees Fahrenheit, 10 mile-per-hour wind—14 minutes



amperes melting current is available, the melting must be done while the air temperature is near freezing and the wind velocity is low, or it may be impossible to remove the ice. If rising winds and a drop in temperature are forecast, prompt action must be taken to remove the ice while the conditions are most favorable.

In the lower left-hand corner of Figure 1, the current values required for ice prevention at various air temperatures and wind velocities are also shown. The time factor does not enter into these curves as a variable since prevention, if used, is a continuous process during the storm period. These curves show that much smaller currents are required for prevention. Most ice formations on conductors occur when the air temperatures are near −2 degrees centigrade. Assuming a 10-mile-per-hour wind, only 200 amperes are required to prevent the formation of ice, as compared to 550 amperes required to remove 1/4 inch of ice in 10 minutes.

Maximum allowable current data for various size conductors under various conditions of ambient temperature and wind velocity has also been prepared. This data is also used as a guide for preparation of ice-melting routines.

Melting of ice using the short-circuit method dates back to 1923 when current was applied to the 12- and 33-kv river crossing lines at station 9, Joliet, to clear them of a heavy formation. However, it was not until 1932 that ice-melting operations were first put into effect on 66- and 132-kv lines. A severe storm at that time demonstrated the necessity of initiating a regular system of freeing the lines of ice.

Early methods of ice melting called for the use of generators as a source of melting current which has the advantage that the desired current can be applied to lines of various lengths and impedances by varying the generator voltage. A disadvantage of this method is the necessity of reducing the available load carrying generator capacity. A further serious disadvantage is the amount of time consumed in preparing a generator and switching to make it available for this purpose. This method has been abandoned where possible and the simpler method of melting directly from the normal working system bus or with isolated transformers is being employed.

Figure 2 shows an application of the short-circuit method on a 115-mile 132-kv transmission line combination between Waukegan and Chicago Heights. This circuit is isolated from the transmission system at Electric Junction, Joliet, and Chicago Heights. Seven hundred eighty amperes of melting current is supplied directly from the normal 132-kv operating bus at Waukegan Generating Station to the short-circuit disconnects at the Chicago Heights Substation. Since this current is far beyond normal load values, the relaying is made ineffective at all points except Waukegan where overload protection is provided by special relays to protect the operating system. The melting time to remove 1/4 inch of ice from conductors with an ambient temperature of 28 degrees Fahrenheit and 10-mile-per-hour wind is 14 minutes.

There have been numerous applications of the short-circuit method of ice melting on 33-kv lines with the current requirements supplied directly from the substation 33-kv operating bus. In a large number of cases, load interrupter short-circuiting switches are used at the far end of the line which initiate and interrupt the flow of melting current. Their use has expedited considerably the melting operations. Since the voltage level along the line will vary from approximately 33 kv at the source to zero voltage at the short-circuit location, service to customers close to the source will not be materially affected while service to those near the end of the line will be interrupted. As a guide to determine whether intermediate customers should be disconnected from the line during the melting operation, curves shown on Figure 3 prepared by the company testing department show the allowable duration of low voltage without damage to customer motors. The data on these curves is based on tests of only a few motors considered as representative.

Figure 4 shows the ice-melt facilities installed at Bellwood Substation to provide 3-, 4-, 12- and 33-kv potential for short-circuit melting on 33- and 66-kv transmission lines in the area. Due to some lines being relatively shorter than others, 3 and 4 kv is applied to give the desired current. Longer lines, or two or more shorter lines placed in series, require 12- and 33-kv potential. Three or 4 kv is available from a 15,000-kva, 33/4-kv transformer, which is normally used to supply a portion of the 4-kv local load. Twelve and 33 kv is available from either of two 60,000-kva 66/33-12-kv trans-

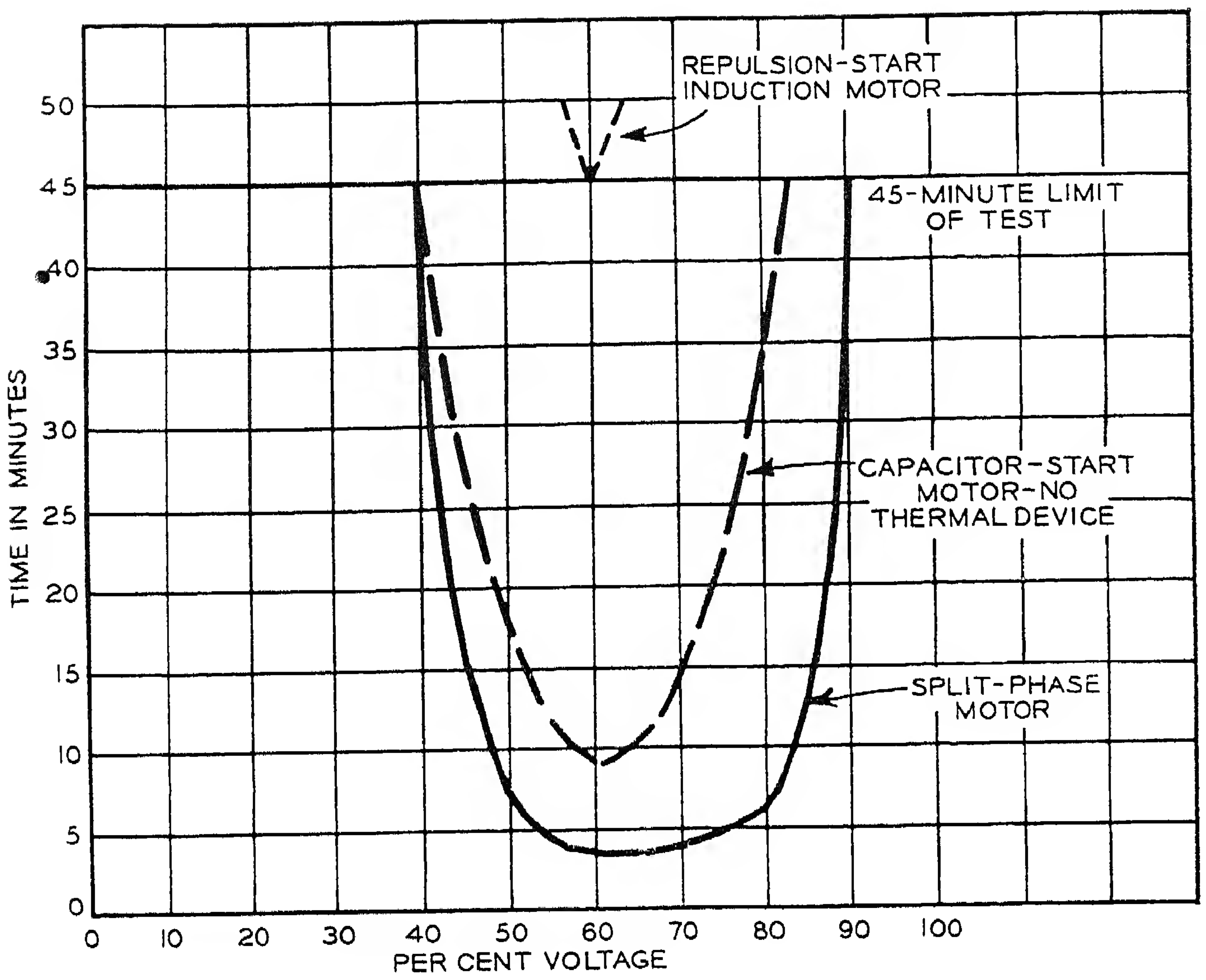


Figure 3 (above). Time in minutes for motor field windings to reach a total temperature of 125 degrees centigrade under various values of impressed voltage

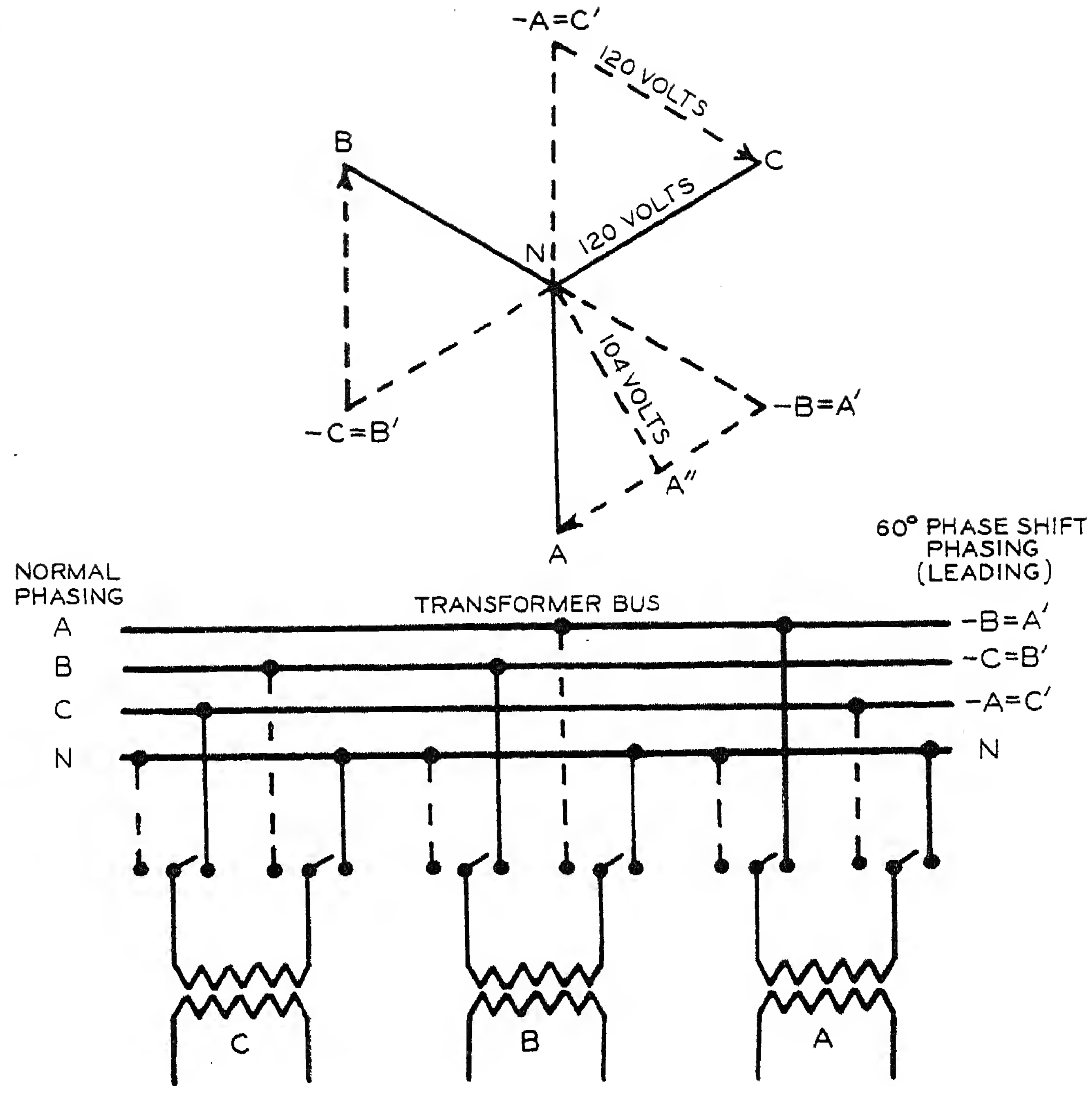


Figure 5. 60 degree phase-shift transformer bank connections

Solid lines—normal connections
Dotted lines—60-degree phase shift

formers which normally carry the local 12- and 33-kv load requirements. This arrangement affords great flexibility as to melting potential and source.

Phase-Shift Method

Use of the phase-shift or out-of-phase method of melting ice from the company's transmission lines has become quite popular because it can be used without interferring with service to the lines generally. Essentially this method consists of changing the connections on a bank of transformers by means of disconnects to create a phase angle difference between it and other standard connected transformer banks. Circulating current can then be caused to flow over a transmission line connecting the standard bank and the "phase shifted bank." The amount of circulating current will depend on the line impedance and the degree of phase shift.

Figure 5 shows how a transformer bank can be reconnected for a 60-degree phase shift by use of six single blade double-throw disconnects. The vector diagrams show the relation between the standard connected bank and the phase-shifted bank. The out-of-phase voltage impressed on a transmission line connecting a 60-degree phase shifted bank and a standard bank is equal to the phase to neutral secondary voltage of the transformer bank. For a 33-kv system, this is approximately 19 kv.

Sixty, 120- or 180-degree phase shifts are available at the company's Electric Junction Substation for ice melting. One hundred twenty-degree shift with respect to Waukegan Station 33-kv system is accomplished at Electric Junction by a roll in the 33-kv cable connecting a standard 132/33-12-kv transformer bank and the 132-kv transfer bus to which the Waukegan 132-kv line is switched for ice melting at 33 kv. The 180-degree departure with respect to the Waukegan Station 33-kv system is accomplished by use of the

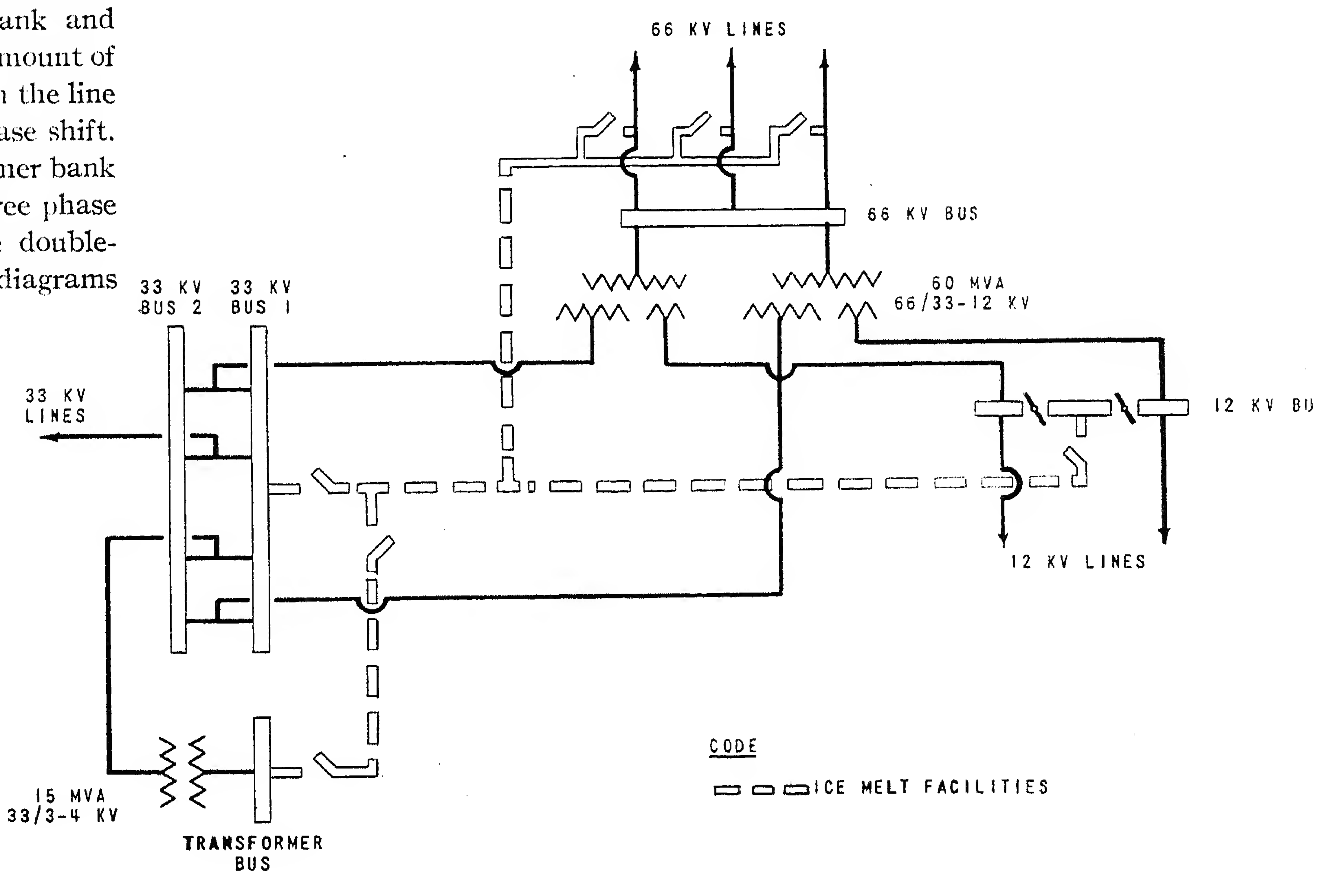


Figure 4. Bellwood Substation facilities to melt ice on transmission lines at 3-, 4-, 12- and 33-kv

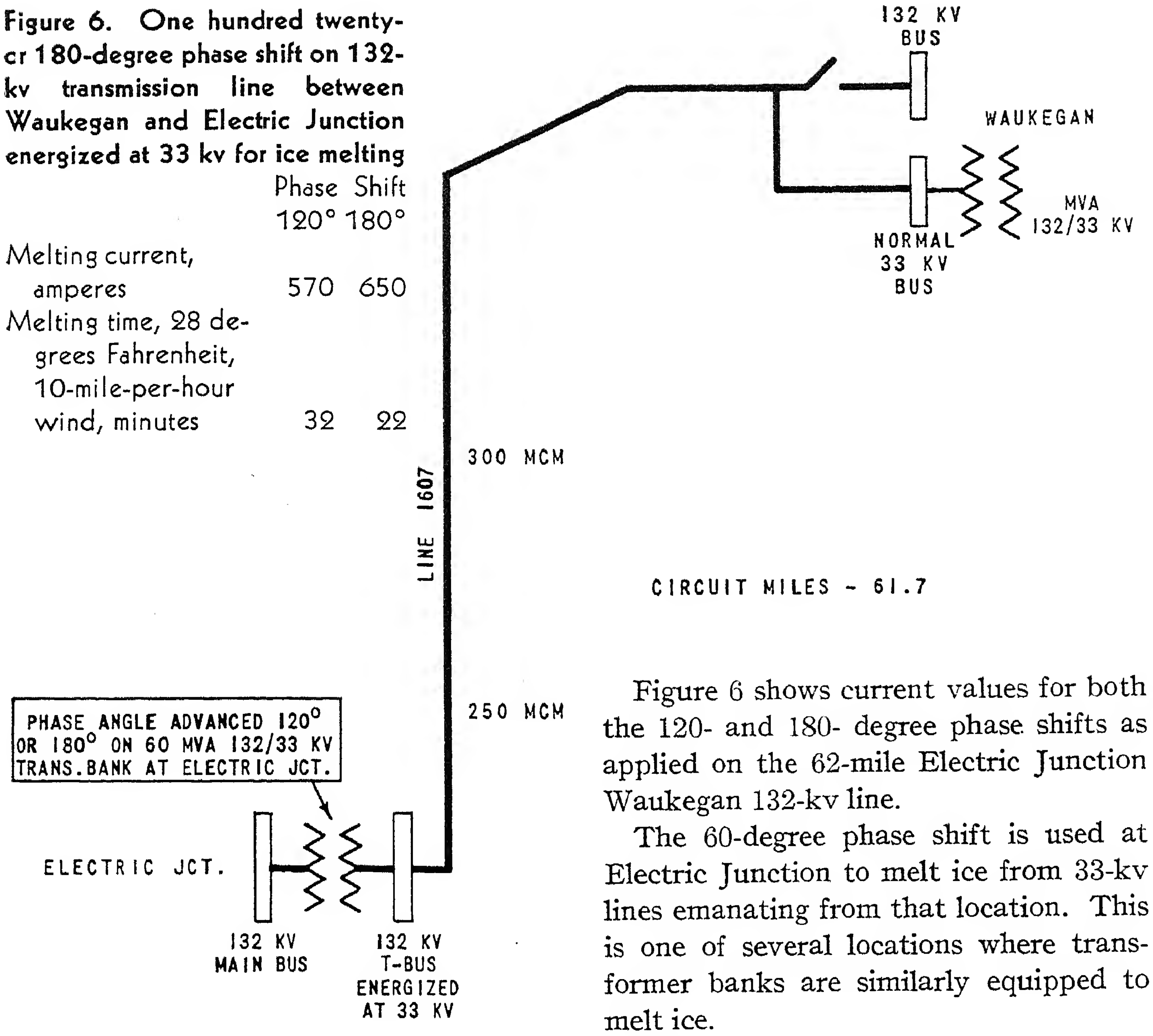


Figure 6. One hundred twenty- or 180-degree phase shift on 132-kv transmission line between Waukegan and Electric Junction energized at 33 kv for ice melting

	Phase Shift 120°	Phase Shift 180°
Melting current, amperes	570	650
Melting time, 28 degrees Fahrenheit, 10-mile-per-hour wind, minutes	32	22

60-degree double-throw disconnects and two rolls in the cable connection between the shifted bank and the 132-kv transfer bus. The phase angle thus created between the Electric Junction and Waukegan 33-kv systems causes circulating current to flow over the 132-kv line connecting the two systems.

Figure 6 shows current values for both the 120- and 180- degree phase shifts as applied on the 62-mile Electric Junction Waukegan 132-kv line.

The 60-degree phase shift is used at Electric Junction to melt ice from 33-kv lines emanating from that location. This is one of several locations where transformer banks are similarly equipped to melt ice.

Figure 7 shows an application of the 60-degree phase-shift method of removing ice from a 41-mile 33-kv transmission line combination between Electric Junction and Bellwood Substations. Normal service to customers supplied from the circuit is maintained throughout the melting operation.

Use of the 60-degree phase shift on 33-kv lines from which customers are supplied has two very important advantages over the short-circuit method. The first is that service to customers connected to the line can be maintained at operatable voltage levels while the melting current is applied, the minimum voltage level being approximately 87 per cent of normal voltage. The second advantage in the use of this method is the considerable reduction in the amount of preparatory line switching required, reducing the elapsed time for removing ice, and permitting a wider coverage of melting operations before a drop in ambient temperature and increased wind velocities make such operations more difficult.

The phase-shift method of ice removal from lines unfolds numerous possibilities for application to line combinations of like voltage. It also makes possible application on different voltage circuits placed in series for the melting operation. Figure 8 shows such an application on a 33-kv transmission line 12-kv feeder loop, one end of which is connected to a standard transformer bank and the other to a phase shifted transformer bank at Waukegan Station. This method effectively melts ice from the 12-kv feeder.

Conclusions

Approximately 95 per cent of the company's 3,200 circuit miles of 33-, 66- and 132-kv transmission lines is covered by short-circuit and phase-shift ice-melting operations. In order to obtain this coverage, installation of special facilities was required. It was necessary to check the capacity of existing substation equipment to determine its adequacy to carry the overload current duty imposed during the melting operation. Transmission lines had to be checked and in some cases small size conductors were replaced with larger conductors in order to have as uniform a conductor size as possible throughout the line to prevent excessive heating in the smaller size sections. Short-circuiting switches were installed on the lines at locations where previously it was

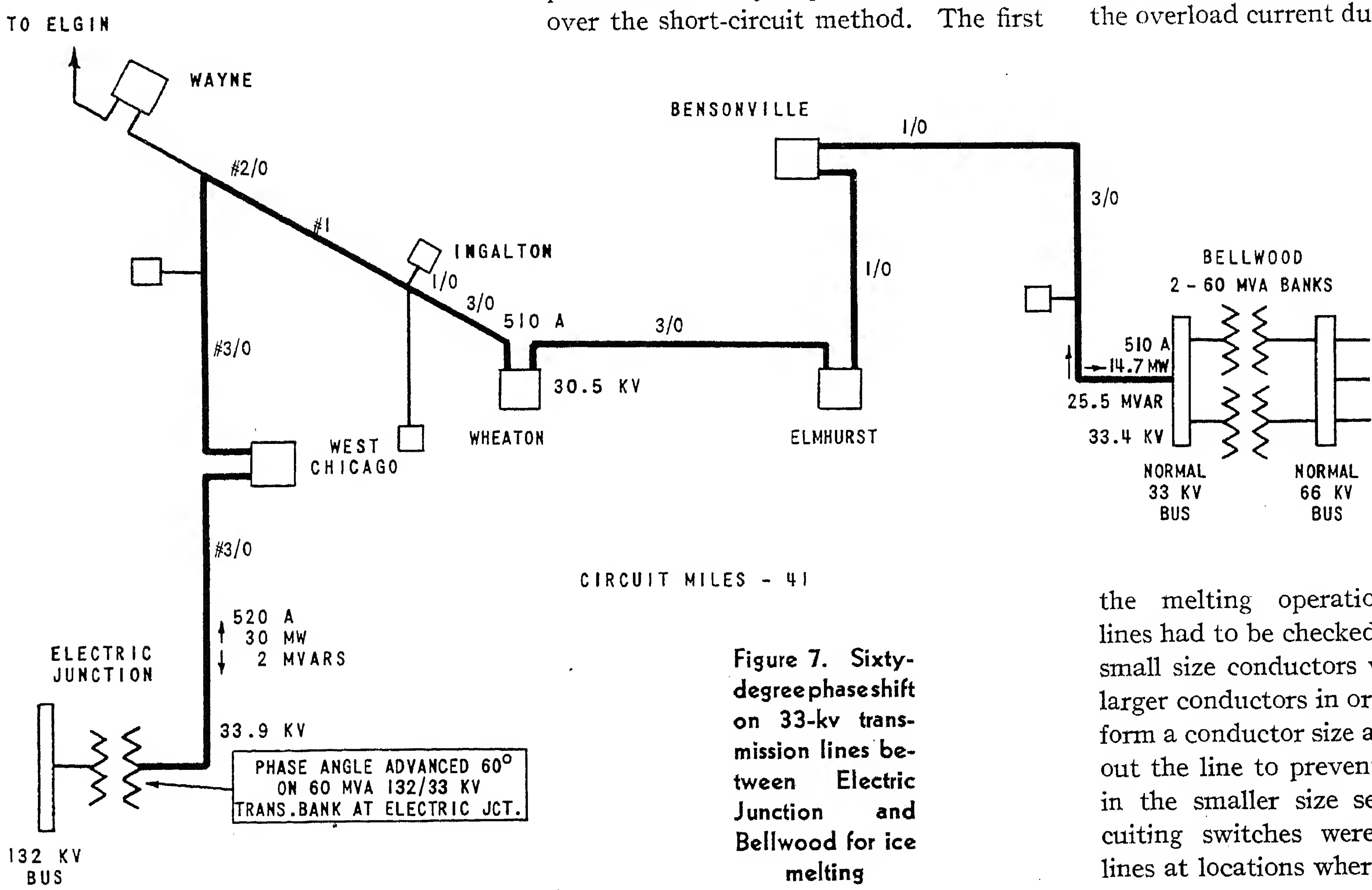


Figure 7. Sixty-degree phase shift on 33-kv transmission lines between Electric Junction and Bellwood for ice melting

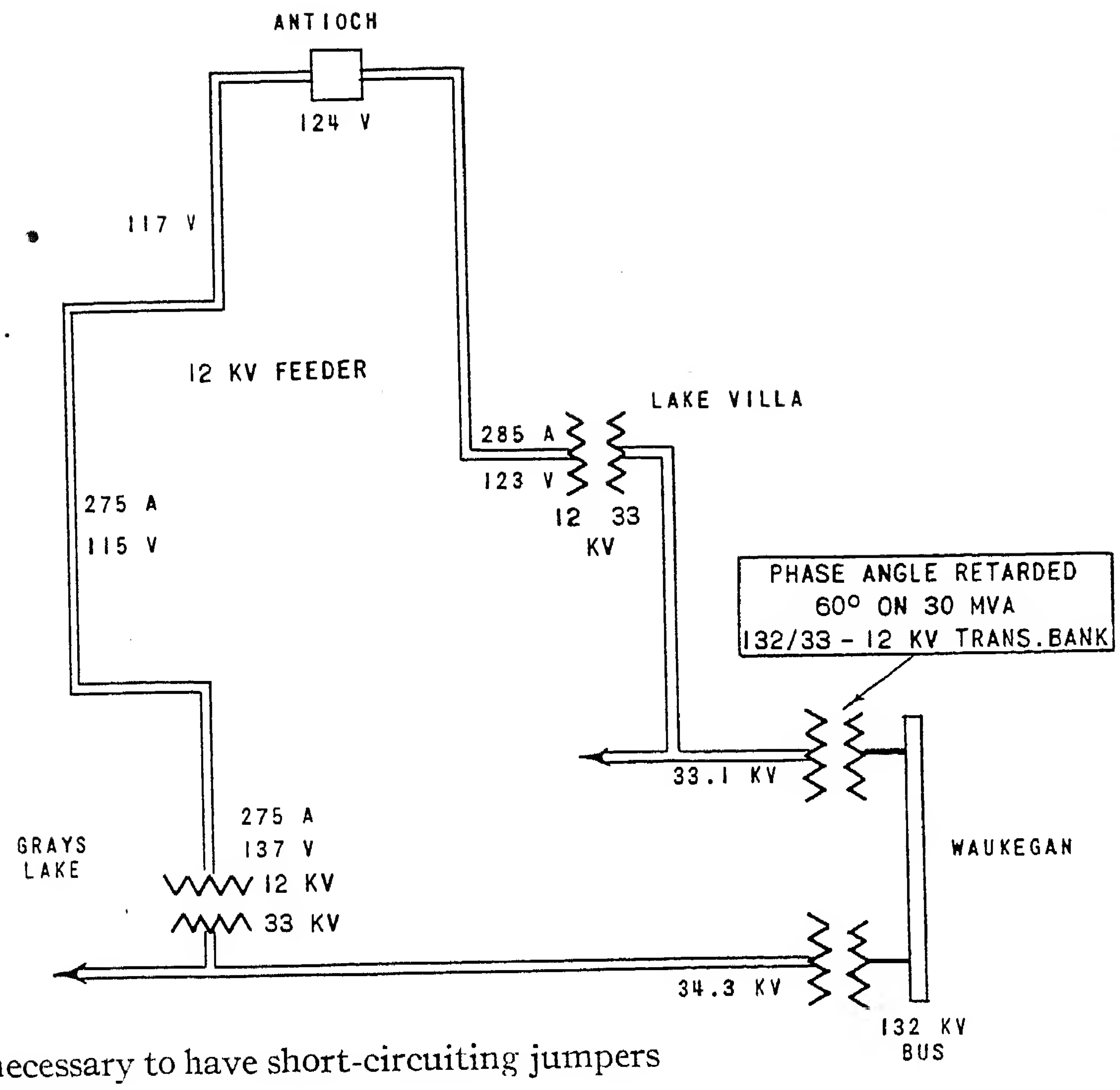


Figure 8. Sixty-degree phase shift on 33-kv transmission lines and 12-kv feeder loop for ice melting

necessary to have short-circuiting jumpers installed.

These modern methods of ice melting would be difficult to use without the radio system of communication. Previous delays in reports from observers stationed along the transmission line during melting operations relative to degree of ice removal have been overcome by use of radio communication. As a result, melting current is immediately removed when the line is clear of ice thus minimizing the possibility of equipment damage from overheating.

While great progress in this activity has been made, many problems still remain to be solved, such as adequate phase-to-phase relay coverage for lines placed in series for combination melting, and the failure of disconnects and air-break switches to function properly when covered with ice. Infrared heat lamps are now being installed in some locations so as to keep the important switches clear of ice.

Consideration has been given to the prevention and removal of ice formation on transmission line static wires which do not lend themselves to any of the regular ice-melting methods. As a step in this direction, facilities have been installed for current application on the static wires of the river crossing lines at station 9, Joliet. For this purpose 240- or 480-volt potential is provided from a distribution feeder.

The challenge of ice formation on transmission lines has been accepted by the company and progress has been made in combating the problem. The challenge still remains, however, and it is essential that continued ingenuity be exercised in the development of new methods to further expedite the removal of ice and safeguard service to customers.

No Discussion

Tables of Binomial Probability Distribution to Six Decimal Places

AIEE COMMITTEE REPORT

THESE tables of binomial probability distribution were prepared for use in calculating reserve requirements in electric power systems and the outage rates p were selected for that purpose. The tables are for groups of m units where m varies from 2 to 65 and for outage rates p as follows:

1. from 0.0050 to 0.0110 in steps of 0.001
2. from 0.0125 to 0.0200 in steps of 0.0025
3. 0.0220
4. from 0.0250 to 0.0400 in steps of 0.0050
5. from 0.0500 to 0.0600 in steps of 0.0100

For the complete range of p from 0.00 to 1.00 at intervals of 0.01 and for m from 2 to 50 reference should be made to the 7-place tables published by the United States Bureau of Standards in 1950.[1]

The method of reserve determination by probability methods proceeds from the assumption that forced outage of generating units are random events independent of one another. Then in a very long elapsed time period the fraction of total elapsed time that forced outage of any given number of units exists or the probability that at any given time forced outage of any given number of units will be found to exist can be obtained from the well-known binomial expansion. If the outage rate or the probability that forced outage of one given unit exists at any given time is p, then $(1-p)=q$ is the probability that forced outage of the unit will not exist, and the successive terms of the binomial expansion

$$(q+p)^m = q^m + C_1{}^m q^{m-1}p + C_2{}^m q^{m-2}p^2 + \dots + C_n{}^m q^{m-n}p^n + \dots + p^m$$

give the probabilities of having 0, 1, 2,..., n, ..., m generating units out of service simultaneously from a total of m units of which $(m-n)$ are operating and n are out of service because of forced outage.

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Personnel of the AIEE Joint Subcommittee on Application of Probability Methods to Power System Problems: G. Calabrese, *Chairman;* W. R. Blakely, V. M. Cook, Howard Duryea, R. G. Hooke, F. J. Leerburger, H. A. Lott, W. J. Lyman, Bela Mohr, H. W. Phillips, H. B. Smith, V. A. Thiemann, and C. W. Watchorn.

The tables in this paper were prepared under the general supervision of Philip Doane of the Consolidated Edison Company of New York, Inc.

Reference

1. TABLES OF THE BINOMIAL PROBABILITY DISTRIBUTION. *Applied Mathematical Series 6*, National Bureau of Standards, United States Government Printing Office, Washington, D. C., January 27, 1950.

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0050

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	990025	985075	980149	975249	970372	965520	960693	955889	951110	946354	941623	936915	932230	927569	922931	918316	0
1	9950	14850	19702	24503	29258	33964	38621	43232	47795	52312	56781	61205	65585	69917	74206	78450	1
2	25	75	149	247	368	512	679	869	1080	1314	1569	1845	2141	2460	2796	3153	2
3				1	2	4	7	10	15	20	27	35	43	53	66	80	3
4													1	1	1	1	4

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	913725	909156	904611	900087	895587	891109	886653	882220	877809	873420	869053	864708	860384	856082	851802	847543	0
1	82648	86804	90915	94985	99009	102992	106934	110832	114689	118504	122279	126012	129707	133360	136973	140547	1
2	3531	3926	4340	4773	5225	5694	6179	6683	7204	7742	8295	8866	9450	10052	10669	11301	2
3	94	112	131	151	175	200	228	258	289	324	361	400	443	488	536	586	3
4	2	2	3	4	4	5	6	7	9	10	12	14	16	18	19	22	4
5															1	1	5

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	843305	839089	834893	830718	826565	822432	818320	814229	810157	806107	802076	798065	794075	790105	786154	782224	0
1	144083	147578	151036	154456	157836	161181	164487	167755	170988	174183	177343	180468	183556	186608	189626	192607	1
2	11946	12607	13282	13971	14674	15389	16117	16860	17615	18382	19161	19951	20753	21567	22393	23230	2
3	640	697	757	819	885	953	1027	1101	1180	1262	1348	1437	1530	1626	1725	1828	3
4	25	28	31	35	39	43	47	53	58	63	69	76	82	90	97	106	4
5	1	1	1	1	1	2	2	2	2	3	3	3	4	4	5	5	5

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	778313	774421	770549	766696	762863	759048	755253	751477	747719	743981	740261	736560	732877	729212	725567	721938	0
1	195555	198470	201349	204196	207007	209788	212534	215247	217929	220577	223195	225779	228334	230857	233348	235810	1
2	24077	24933	25802	26678	27567	28463	29370	30286	31211	32144	33086	34038	34995	35962	36936	37919	2
3	1935	2046	2160	2279	2401	2527	2656	2790	2927	3070	3214	3363	3517	3675	3836	4001	3
4	114	124	133	144	154	165	177	189	203	216	230	245	261	277	294	312	4
5	6	6	7	7	8	9	10	11	10	11	13	14	15	16	18	19	5
6									1	1	1	1	1	1	1	1	6

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0060

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	988036	982108	976215	970358	964536	958748	952996	947278	941594	935945	930329	924747	919199	913684	908201	902752	0
1	11928	17784	23571	29286	34932	40511	46020	51462	56837	62145	67389	72566	77678	82727	87714	92637	1
2	36	108	213	354	528	734	972	1242	1544	1875	2236	2628	3048	3496	3971	4473	2
3			1	2	4	7	12	18	25	35	45	58	74	91	112	135	3
4											1	1	1	2	2	3	4

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	897336	891952	886600	881280	875992	870737	865512	860319	855157	850026	844926	839856	834817	829808	824830	819881	0
1	97497	102296	107034	111712	116330	120887	125387	129827	134210	138536	142805	147018	151175	155277	159323	163316	1
2	5002	5557	6138	6743	7373	8026	8703	9404	10127	10871	11637	12424	13231	14059	14906	15773	2
3	161	190	222	258	296	339	385	435	488	547	608	675	746	820	900	984	3
4	4	5	6	7	9	11	13	15	18	19	23	26	30	35	40	44	4
5										1	1	1	1	1	1	2	5

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	814962	810072	805211	800380	795578	790804	786059	781343	776655	771995	767363	762759	758182	753633	749111	744617	0
1	167255	171142	174976	178757	182486	186166	189794	193371	196899	200377	203807	207188	210522	213808	217047	220239	1
2	16658	17561	18483	19422	20378	21350	22339	23344	24364	25400	26449	27514	28592	29683	30788	31905	2
3	1073	1166	1265	1368	1477	1590	1708	1832	1961	2095	2236	2380	2531	2688	2850	3018	3
4	50	57	62	70	78	86	96	105	115	127	138	151	164	178	193	209	4
5	2	2	3	3	3	4	4	5	6	6	7	8	9	10	11	11	5
6																1	6

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	740149	735708	731294	726906	722545	718209	713900	709617	705359	701127	696920	692739	688582	684450	680344	676262	0
1	223385	226486	229541	232552	235517	238440	241319	244153	246947	249697	252406	255072	257699	260285	262829	265334	1
2	33036	34178	35332	36497	37674	38861	40057	41266	42482	43710	44945	46191	47444	48705	49975	51252	2
3	3191	3369	3554	3745	3941	4144	4353	4567	4787	5013	5246	5483	5727	5978	6234	6497	3
4	226	245	263	282	304	325	348	372	398	423	451	480	510	541	574	607	4
5	12	13	15	17	18	20	22	24	26	28	30	33	36	39	41	45	5
6	1	1	1	1	1	1	1	1	1	2	2	2	2	2	3	3	6

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0070

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	986049	979146	972292	965486	958728	952017	945353	938735	932164	925639	919160	912726	906336	899992	893692	887436	0
1	13902	20708	27417	34031	40551	46978	53313	59558	65712	71777	77753	83643	89448	95166	100799	106350	1
2	49	146	290	480	714	993	1315	1679	2084	2530	3015	3538	4098	4695	5330	5997	2
3			1	3	7	12	19	28	40	53	71	91	116	144	175	212	3
4										1	1	2	2	3	4	5	4

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	881225	875056	868931	862848	856808	850810	844855	838941	833068	827237	821446	815696	809986	804316	793095	787543	0
1	111816	117203	122507	127732	132878	137947	142936	147849	152687	157449	162138	166753	171296	175767	184496	188757	1
2	6700	7435	8204	9005	9836	10696	11587	12507	13455	14429	15430	16458	17509	18585	20810	21955	2
3	252	297	347	402	462	528	599	676	758	848	943	1043	1152	1267	1515	1651	3
4	7	9	11	13	16	18	22	26	31	36	42	48	55	63	81	90	4
5						1	1	1	1	1	1	2	2	2	3	4	5

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	787543	782031	776556	771120	765722	760362	755040	749755	744506	739295	734120	728981	723878	718811	713779	708783	0
1	188757	192948	197072	201129	205119	209043	212902	216696	220428	224096	227702	231247	234732	238156	241521	244826	1
2	21955	23122	24311	25520	26750	27998	29265	30551	31855	33175	34512	35864	37231	38613	40010	41421	2
3	1651	1793	1943	2099	2262	2435	2613	2800	2994	3196	3405	3624	3849	4083	4324	4574	3
4	90	101	113	126	140	154	171	188	206	225	247	268	292	317	344	371	4
5	4	5	5	6	7	8	9	10	10	12	13	15	17	19	21	24	5
6									1	1	1	1	1	1	1	1	6

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	703821	698894	694002	689144	684320	679530	674774	670050	665359	660702	656077	651485	646924	642396	637899	633433	0
1	248074	251265	254398	257475	260497	263464	266375	269234	272041	274793	277495	280144	282744	285293	287793	290245	1
2	42845	44281	45730	47191	48662	50145	51639	53143	54655	56177	57707	59246	60792	62345	63906	65473	2
3	4832	5099	5373	5655	5947	6245	6552	6867	7191	7524	7864	8213	8571	8937	9310	9692	3
4	401	431	464	499	534	572	612	654	698	743	790	840	891	945	1001	1059	4
5	26	28	31	34	38	41	45	49	52	57	63	67	73	78	85	91	5
6	1	2	2	2	2	3	3	3	4	4	4	5	5	6	6	7	6

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0080

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	984064	976192	968382	960635	952950	945326	937764	930262	922820	915437	908114	900849	893641	886492	879400	872365	0
1	15872	23617	31238	38735	46110	53366	60500	67518	74420	81208	87881	94443	100896	107238	113472	119599	1
2	64	190	378	625	930	1290	1708	2179	2701	3274	3898	4570	5289	6053	6863	7716	2
3		1	2	5	10	18	28	40	58	80	105	135	170	212	258	311	3
4								1	1	1	2	3	4	5	7	9	4

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	865387	858464	851595	844783	838025	831320	824670	818073	811528	805035	798595	792206	785869	779582	773345	767159	0
1	125620	131538	137355	143068	148681	154197	159613	164933	170159	175291	180329	185275	190130	194895	199574	204163	1
2	8611	9547	10523	11538	12591	13679	14803	15962	17153	18377	19632	20918	22232	23577	24946	26343	2
3	370	436	509	589	676	772	876	987	1107	1235	1372	1518	1674	1838	2012	2196	3
4	12	15	18	21	26	31	37	43	51	60	69	80	91	103	117	132	4
5				1	1	1	1	2	2	2	3	3	4	5	6	7	5

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	761021	754933	748894	742903	736959	731064	725215	719414	713659	707949	702285	696667	691094	685565	680080	674640	0
1	208668	213086	217420	221672	225843	229931	233941	237870	241722	245498	249199	252823	256373	259851	263257	266591	1
2	27766	29214	30685	32178	33694	35231	36788	38366	39962	41577	43207	44856	46519	48198	49892	51599	2
3	2388	2591	2804	3028	3261	3505	3759	4022	4297	4582	4878	5184	5503	5831	6169	6519	3
4	149	167	187	207	230	254	280	309	338	369	403	439	477	517	560	604	4
5	8	9	10	12	12	14	16	18	21	24	27	29	32	36	40	44	5
6					1	1	1	1	1	1	1	2	2	2	2	3	6

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	669243	663889	658578	653309	648083	642898	637754	632652	627592	622570	617590	612650	607748	602886	598063	593279	0
1	269855	273050	276177	279237	282229	285156	288019	290817	293550	296224	298834	301383	303874	306305	308678	310992	1
2	53319	55051	56795	58549	60315	62090	63875	65668	67469	69278	71094	72916	74744	76577	78414	80257	2
3	6879	7252	7633	8027	8431	8847	9272	9709	10157	10615	11084	11564	12055	12557	13069	13591	3
4	653	701	755	810	867	927	991	1057	1127	1199	1274	1353	1434	1518	1607	1699	4
5	48	54	58	64	70	77	83	90	97	106	115	124	134	145	156	168	5
6	3	3	4	4	5	5	6	7	8	7	8	9	10	11	12	13	6
7										1	1	1	1	1	1	1	7

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0090

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	982081	973242	964483	955803	947200	938676	930228	921856	913559	905337	897189	889114	881112	873182	865323	857535	0
1	17838	26517	35037	43401	51614	59673	67584	75348	82967	90442	97776	104971	112028	118950	125739	132395	1
2	81	240	477	789	1172	1626	2148	2737	3390	4107	4884	5720	6614	7562	8564	9619	2
3		1	3	7	14	25	40	58	83	112	148	191	240	298	363	437	3
4								1	1	2	3	4	6	8	11	14	4

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	849818	842170	834590	827079	819635	812258	804948	797703	790524	783409	776358	769371	762447	755585	748785	742045	0
1	138920	145318	151590	157737	163761	169665	175447	181114	186662	192098	197420	202630	207730	212722	217608	222390	1
2	10724	11878	13079	14325	15616	16949	18324	19737	21191	22679	24204	25763	27355	28979	30632	32314	2
3	520	611	713	824	946	1077	1221	1375	1539	1716	1905	2106	2319	2544	2782	3033	3
4	18	22	27	34	41	49	58	68	81	94	108	124	142	162	183	207	4
5		1	1	1	1	2	2	3	3	4	5	6	7	8	10	11	5

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	735367	728749	722190	715690	709249	702866	696541	690271	684059	677902	671801	665755	659763	653826	647941	642110	0
1	227066	231641	236115	240490	244766	248946	253031	257024	260922	264731	268450	272080	275623	279079	282452	285741	1
2	34026	35762	37526	39313	41124	42957	44810	46684	48578	50489	52416	54360	56320	58294	60282	62281	2
3	3296	3573	3862	4165	4482	4811	5155	5512	5882	6266	6665	7077	7502	7941	8394	8861	3
4	232	260	289	322	356	394	433	475	521	569	620	674	733	794	858	926	4
5	12	14	17	19	22	25	28	32	36	41	45	51	55	62	68	76	5
6	1	1	1	1	1	1	2	2	2	2	3	3	4	4	5	5	6

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	636330	630603	624928	619304	613730	608206	602733	597308	591932	586605	581326	576093	570908	565770	560679	555633	0
1	288950	292076	295123	298090	300981	303797	306536	309202	311795	314316	316766	319148	321461	323706	325883	327996	1
2	64291	66314	68345	70387	72437	74493	76556	78626	80701	82781	84865	86952	89042	91134	93227	95321	2
3	9343	9837	10345	10867	11402	11952	12515	13092	13682	14284	14901	15531	16173	16828	17498	18179	3
4	997	1072	1151	1234	1321	1411	1506	1604	1708	1817	1928	2045	2167	2293	2423	2560	4
5	83	91	100	110	120	130	142	155	168	181	196	212	228	246	265	283	5
6	6	7	8	7	8	10	11	12	13	15	17	18	19	21	23	26	6
7				1	1	1	1	1	1	1	1	1	2	2	2	2	7

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0100

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	980100	970299	960596	950990	941480	932065	922745	913517	904382	895338	886385	877521	868746	860059	851458	842943	0
1	19800	29403	38812	48030	57060	65904	74565	83047	91352	99482	107441	115230	122852	130311	137609	144748	1
2	100	297	588	970	1440	1997	2636	3356	4152	5025	5968	6984	8067	9214	10425	11697	2
3		1	4	10	20	34	53	79	112	152	201	258	326	403	491	591	3
4							1	1	2	3	5	7	9	13	17	20	4
5																1	5

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	834514	826169	817907	809728	801630	793614	785678	777822	770043	762343	754719	747172	739701	732303	724980	717730	0
1	151729	158557	165234	171760	178141	184376	190467	196419	202234	207911	213456	218868	224151	229308	234337	239244	1
2	13028	14414	15855	17350	18893	20485	22126	23808	25534	27302	29108	30952	32830	34743	36690	38666	2
3	702	826	961	1110	1273	1449	1639	1844	2064	2298	2548	2814	3095	3393	3706	4036	3
4	26	33	42	50	61	73	86	102	119	139	161	184	211	239	271	305	4
5	1	1	1	2	2	3	4	5	6	7	8	10	12	13	15	18	5
6														1	1	1	6

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	710553	703448	696413	689449	682555	675729	668972	662282	655659	649102	642612	636185	629823	623525	617290	611117	0
1	244029	248693	253242	257673	261990	266196	270291	274279	278159	281934	285605	289176	292646	296018	299293	302473	1
2	40671	42705	44764	46849	48958	51088	53240	55409	57598	59804	62025	64261	66510	68771	71043	73326	2
3	4382	4745	5125	5522	5934	6365	6811	7276	7758	8256	8771	9304	9854	10420	11004	11604	3
4	343	384	427	474	525	579	637	698	763	834	908	986	1070	1158	1250	1348	4
5	21	24	27	31	36	41	46	53	59	66	74	82	90	100	112	122	5
6	1	1	2	2	2	2	3	3	4	4	5	6	7	8	7	9	6
7															1	1	7

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	605006	598956	592966	587037	581166	575355	569601	563905	558266	552683	547156	541685	536268	530906	525596	520341	0
1	305559	308553	311458	314272	317000	319641	322199	324673	327066	329378	331611	333765	335845	337848	339780	341637	1
2	75618	77917	80224	82536	84854	87175	89500	91827	94154	96484	98813	101142	103467	105791	108112	110428	2
3	12221	12856	13505	14173	14856	15557	16272	17004	17753	18517	19297	20092	20903	21729	22569	23425	3
4	1450	1558	1671	1789	1914	2042	2178	2319	2466	2618	2777	2943	3114	3292	3476	3667	4
5	135	148	162	178	193	211	229	249	269	291	314	338	365	392	421	452	5
6	10	11	13	14	16	18	19	21	24	27	30	32	35	39	42	46	6
7	1	1	1	1	1	1	2	2	2	2	2	3	3	3	4	4	7
8																	8

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0110

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	978121	967362	956721	946197	935789	925495	915314	905246	895288	885440	875700	866068	856541	847119	837801	828585	0
1	21758	32277	42563	52619	62448	72055	81444	90616	99578	108330	116879	125225	133374	141329	149093	156668	1
2	121	360	711	1171	1737	2405	3171	4032	4983	6025	7149	8356	9642	11003	12436	13941	2
3		1	5	13	26	45	70	104	148	200	265	341	430	531	646	775	3
4							1	2	3	5	7	10	13	18	23	30	4
5															1	1	5

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	819470	810456	801541	792724	784004	775380	766851	758415	750073	741822	733663	725592	717610	709717	701910	694189	0
1	164060	171270	178301	185156	191840	198354	204700	210885	216907	222772	228481	234038	239446	244705	249820	254793	1
2	15510	17144	18839	20594	22403	24267	26183	28146	30156	32211	34306	36443	38616	40825	43068	45343	2
3	921	1080	1257	1451	1662	1889	2136	2400	2684	2985	3307	3648	4009	4390	4790	5211	3
4	38	48	60	72	87	105	124	147	171	199	230	264	301	341	387	434	4
5	1	2	2	3	4	5	6	7	9	11	12	14	17	21	24	28	5
6											1	1	1	1	1	2	6
7																	7
8																	8

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	686553	679001	671532	664145	656839	649614	642468	635401	628412	621499	614663	607901	601214	594601	588061	581592	0
1	259626	264322	268884	273313	277613	281784	285830	289753	293555	297239	300805	304258	307599	310828	313949	316964	1
2	47647	49979	52336	54718	57123	59548	61993	64455	66933	69426	71932	74450	76977	79514	82058	84610	2
3	5652	6114	6597	7100	7623	8168	8734	9320	9926	10553	11201	11869	12557	13266	13995	14743	3
4	488	544	605	672	742	818	898	984	1076	1173	1277	1386	1501	1623	1751	1886	4
5	32	38	43	49	56	64	72	81	91	102	113	126	141	155	171	188	5
6	2	2	3	3	4	4	5	6	7	8	8	9	10	12	14	16	6
7											1	1	1	1	1	1	7
8																	8

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	575194	568867	562610	556421	550300	544247	538260	532339	526484	520692	514964	509300	503698	498157	492678	487258	0
1	319876	322684	325391	328001	330514	332931	335257	337490	339632	341688	343658	345542	347342	349063	350702	352264	1
2	87165	89725	92288	94852	97417	99981	102542	105102	107659	110211	112757	115296	117830	120354	122870	125377	2
3	15512	16300	17108	17935	18780	19646	20530	21432	22352	23290	24246	25220	26211	27219	28243	29284	3
4	2027	2176	2331	2493	2664	2840	3025	3218	3419	3627	3843	4068	4300	4540	4791	5048	4
5	207	227	249	272	296	323	350	379	410	444	479	515	555	596	639	685	5
6	18	20	21	24	27	30	33	37	40	44	48	54	58	65	70	76	6
7	1	1	2	2	2	2	3	3	4	4	5	5	6	6	6	7	7
8															1	1	8

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0125

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	975156	962967	950930	939043	927305	915714	904267	892964	881802	870780	859895	849146	838532	828050	817699	807478	0
1	24688	36568	48148	59433	70428	81139	91572	101730	111620	121247	130616	139733	148600	157225	165610	173762	1
2	156	463	914	1505	2229	3081	4057	5151	6359	7674	9094	10612	12227	13931	15723	17595	2
3		2	8	19	38	65	102	152	214	291	384	493	619	764	929	1114	3
4						1	2	3	5	8	11	16	21	29	38	49	4
5													1	1	1	2	5

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	797385	787417	777575	767855	758257	748779	739419	730176	721049	712036	703135	694346	685667	677096	668632	660274	0
1	181682	189379	196854	204113	211150	217998	224633	231069	237307	243354	249213	254887	260379	265696	270839	275811	1
2	19548	21575	23672	25837	28065	30355	32700	35098	37549	40045	42587	45169	47792	50448	53139	55861	2
3	1320	1548	1798	2072	2369	2689	3036	3407	3802	4225	4672	5146	5646	6174	6726	7306	3
4	63	78	97	118	142	170	201	237	277	320	369	424	483	546	618	694	4
5	2	3	4	5	7	9	11	12	15	19	23	27	31	38	43	51	5
6								1	1	1	1	1	2	2	3	3	6

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	652021	643870	635822	627874	620026	612276	604622	597065	589602	582231	574953	567766	560669	553661	546740	539906	0
1	280617	285260	289742	294068	298241	302262	306138	309868	313458	316911	320228	323412	326466	329393	332196	334878	1
2	58609	61385	64183	67003	69841	72697	75566	78448	81341	84242	87150	90064	92981	95899	98819	101736	2
3	7914	8547	9208	9895	10608	11349	12116	12910	13728	14574	15444	16340	17262	18209	19180	20175	3
4	776	866	962	1065	1175	1293	1419	1552	1694	1844	2004	2172	2349	2536	2731	2937	4
5	59	67	78	89	101	114	129	145	163	182	203	225	250	276	304	334	5
6	4	5	5	6	8	8	9	11	13	15	17	20	21	24	28	31	6
7						1	1	1	1	1	1	1	2	2	2	3	7

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	533157	526493	519911	513413	506995	500657	494399	488220	482117	476090	470139	464263	458459	452728	447069	441481	0
1	337441	339887	342221	344441	346554	348560	350461	352259	353958	355561	357067	358480	359803	361037	362183	363244	1
2	104650	107559	110463	113360	116248	119127	121995	124851	127694	130522	133336	136132	138912	141672	144414	147136	2
3	21195	22239	23305	24394	25506	26641	27797	28974	30173	31392	32631	33890	35167	36464	37780	39112	3
4	3152	3378	3613	3860	4117	4383	4662	4951	5252	5563	5885	6220	6566	6924	7292	7674	4
5	367	401	440	479	521	567	613	665	718	775	835	897	964	1034	1108	1186	5
6	35	40	43	49	54	59	67	73	80	88	97	106	116	127	138	149	6
7	3	3	4	4	5	6	5	6	7	8	9	11	12	13	15	16	7
8							1	1	1	1	1	1	1	1	1	2	8

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0150

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	970225	955671	941336	927216	913308	899609	886114	872823	859730	846834	834132	821620	809295	797156	785199	773421	0
1	29550	43661	57341	70601	83450	95897	107954	119625	130924	141856	152431	162655	172541	182092	191318	200225	1
2	225	665	1310	2150	3177	4381	5753	7287	8972	10801	12766	14863	17079	19411	21850	24394	2
3		3	13	33	64	111	176	259	364	494	648	830	1040	1280	1553	1857	3
4					1	2	3	6	10	15	22	31	44	59	77	99	4
5											1	1	1	2	3	4	5

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	761820	750392	739136	728050	717128	706372	695776	685339	675059	664933	654959	645135	635458	625927	616537	607290	0
1	208823	217119	225118	232827	240257	247409	254294	260917	267283	273400	279273	284908	290311	295487	300445	305185	1
2	27031	29757	32568	35457	38417	41444	44533	47680	50878	54124	57413	60741	64104	67497	70917	74360	2
3	2195	2568	2976	3419	3900	4418	4974	5566	6199	6869	7578	8325	9111	9936	10799	11702	3
4	125	157	192	234	282	337	398	467	543	628	721	824	937	1060	1193	1336	4
5	6	7	10	12	15	19	24	29	36	43	53	63	74	87	102	118	5
6				1	1	1	1	2	2	3	3	4	5	6	7	8	6
7																1	7

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	598180	589207	580369	571664	563088	554642	546323	538128	530056	522105	514273	506559	498961	491476	484104	476843	0
1	309718	314045	318172	322104	325849	329407	332785	335988	339020	341886	344590	347135	349526	351768	353863	355817	1
2	77822	81301	84792	88293	91800	95311	98822	102332	105837	109334	112822	116299	119762	123208	126637	130044	2
3	12641	13618	14634	15687	16775	17901	19062	20258	21490	22755	24054	25385	26748	28144	29570	31026	3
4	1492	1660	1839	2030	2236	2453	2685	2931	3190	3465	3754	4059	4379	4714	5066	5434	4
5	136	156	179	204	231	262	295	330	369	412	458	507	560	618	679	744	5
6	10	12	14	17	20	22	26	31	35	40	45	51	58	66	74	84	6
7	1	1	1	1	1	2	2	2	3	3	4	5	6	6	6	7	7
8															1	1	8

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	469690	462645	455705	448870	442136	435505	428972	422537	416199	409956	403807	397750	391783	385907	380118	374417	0
1	357633	359313	360863	362285	363585	364762	365824	366772	367608	368337	368960	369483	369908	370235	370471	370615	1
2	133431	136794	140132	143443	146726	149979	153200	156389	159545	162666	165752	168800	171810	174781	177713	180604	2
3	32511	34025	35567	37135	38729	40349	41994	43662	45353	47066	48800	50554	52328	54121	55930	57757	3
4	5817	6218	6635	7069	7520	7988	8474	8976	9497	10034	10590	11163	11753	12362	12989	13633	4
5	815	890	970	1055	1145	1241	1341	1449	1562	1681	1806	1938	2077	2222	2373	2533	5
6	93	104	115	129	143	158	174	192	210	231	252	275	300	327	356	385	6
7	9	10	12	13	15	16	19	21	23	26	30	33	37	40	45	50	7
8	1	1	1	1	1	2	2	2	3	3	3	4	4	5	4	5	8
9															1	1	9

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0175

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	965306	948413	931816	915509	899488	883747	868281	853086	838157	823490	809079	794920	781009	767341	753912	740719	0
1	34388	50679	66389	81534	96128	110187	123725	136755	149291	161345	172932	184065	194755	205015	214856	224289	1
2	306	903	1774	2905	4281	5888	7713	9743	11965	14369	16942	19671	22548	25561	28702	31960	2
3		5	21	52	102	175	275	405	569	768	1005	1285	1606	1973	2386	2846	3
4					1	3	6	11	18	27	41	57	79	106	138	177	4
5										1	1	2	3	4	6	9	5

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	727756	715021	702508	690214	678135	666268	654608	643152	631897	620839	609974	599300	588812	578508	568384	558437	0
1	233327	241979	250257	258171	265733	272949	279833	286392	292635	298572	304212	309562	314632	319431	323965	328242	1
2	35326	38790	42346	45985	49698	53479	57319	61213	65153	69134	73149	77194	81261	85344	89440	93545	2
3	3355	3915	4526	5188	5901	6668	7487	8359	9285	10262	11293	12374	13509	14694	15931	17217	3
4	224	280	343	416	499	593	700	819	951	1097	1257	1433	1624	1832	2057	2300	4
5	12	14	19	25	32	41	50	61	74	90	107	127	150	176	205	238	5
6		1	1	1	2	2	3	4	5	6	8	9	11	14	17	20	6
7												1	1	1	1	1	7

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	548664	539063	529630	520361	511254	502308	493517	484881	476395	468058	459868	451820	443913	436144	428512	421013	0
1	332271	336057	339609	342935	346041	348931	351616	354098	356388	358468	360404	362145	363715	365119	366361	367449	1
2	97652	101758	105858	109949	114026	118086	122125	126143	130131	134090	138018	141910	145763	149578	153350	157077	2
3	18553	19937	21369	22847	24371	25941	27554	29208	30905	32642	34417	36229	38079	39963	41882	43833	3
4	2561	2841	3141	3459	3799	4158	4540	4942	5367	5814	6283	6776	7292	7830	8392	8978	4
5	274	314	357	407	460	519	582	652	726	808	895	990	1090	1199	1315	1439	5
6	23	28	33	39	45	52	60	69	80	91	104	117	133	150	168	188	6
7	2	2	3	3	4	5	6	6	7	8	10	12	14	15	18	21	7
8								1	1	1	1	1	1	2	2	2	8

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	413645	406406	399294	392306	385442	378696	372069	365558	359160	352875	346700	340633	334671	328815	323060	317406	0
1	368387	369179	369830	370346	370729	370987	371122	371138	371042	370833	370518	370101	369587	368975	368273	367482	1
2	160758	164392	167976	171509	174988	178414	181784	185097	188352	191550	194688	197765	200780	203734	206626	209455	2
3	45814	47826	49866	51932	54025	56142	58282	60444	62625	64825	67042	69276	71525	73787	76061	78346	3
4	9589	10222	10880	11563	12269	13000	13754	14534	15337	16165	17017	17892	18791	19714	20660	21630	4
5	1571	1712	1861	2018	2186	2361	2548	2744	2951	3167	3395	3633	3882	4144	4416	4700	5
6	210	233	259	288	318	351	386	423	464	508	554	604	658	713	774	837	6
7	24	27	31	34	38	44	49	55	61	68	76	85	93	104	114	126	7
8	2	3	3	4	5	5	5	6	7	8	9	10	12	12	14	16	8
9							1	1	1	1	1	1	1	2	2	2	9

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0200

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	960400	941192	922368	903921	885842	868126	850763	833748	817073	800731	784717	769022	753642	738569	723797	709321	0
1	39200	57624	75296	92237	108471	124017	138900	153137	166749	179756	192175	204027	215326	226093	236343	246092	1
2	400	1176	2304	3764	5534	7593	9922	12501	15314	18343	21571	24983	28564	32298	36175	40179	2
3		8	32	77	151	259	404	595	834	1123	1467	1869	2332	2857	3445	4099	3
4				1	2	5	11	19	29	46	68	95	130	175	228	293	4
5									1	1	2	4	6	8	12	15	5
6																1	6

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	695135	681232	667608	654256	641171	628347	615780	603464	591395	579568	567976	556616	545485	534574	523883	513406	0
1	255356	264152	272493	280395	287872	294939	301607	307891	313802	319353	324558	329427	333969	338201	342128	345762	1
2	44297	48518	52831	57224	61687	66211	70785	75402	80052	84726	89419	94122	98828	103531	108224	112902	2
3	4821	5611	6468	7396	8393	9458	10594	11797	13069	14410	15816	17287	18824	20424	22087	23810	3
4	369	457	561	679	814	965	1135	1324	1534	1764	2017	2294	2594	2918	3267	3644	4
5	21	29	37	47	59	75	93	114	138	166	197	233	275	321	374	431	5
6	1	1	2	3	4	5	6	8	9	12	16	20	23	29	34	42	6
7									1	1	1	1	2	2	3	3	7

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	503138	493074	483213	473549	464078	454796	445700	436786	428051	419490	411100	402878	394821	386924	379186	371602	0
1	349115	352197	355014	357577	359897	361981	363837	365475	366900	368123	369151	369989	370647	371131	371446	371601	1
2	117559	122190	126790	131356	135880	140360	144793	149173	153500	157767	161974	166119	170195	174204	178143	182010	2
3	25591	27431	29326	31275	33276	35329	37429	39577	41768	44004	46278	48592	50943	53328	55746	58193	3
4	4048	4478	4938	5425	5942	6488	7066	7672	8311	8980	9681	10413	11176	11972	12798	13657	4
5	496	567	644	731	825	928	1038	1159	1289	1429	1581	1742	1916	2101	2299	2509	5
6	49	58	69	79	93	107	124	142	162	185	209	237	267	300	336	375	6
7	4	5	6	7	8	10	12	15	17	20	24	27	32	36	41	48	7
8				1	1	1	1	1	2	2	2	3	3	4	5	4	8
9																1	9

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	364170	356886	349749	342754	335899	329180	322597	316145	309822	303626	297553	291602	285770	280055	274454	268965	0
1	371601	371454	371161	370733	370173	369489	368682	367761	366728	365590	364351	363015	361586	360070	358469	356789	1
2	185801	189516	193156	196716	200196	203595	206914	210148	213301	216369	219354	222254	225070	227799	230446	233006	2
3	60670	63173	65699	68248	70818	73406	76009	78628	81258	83899	86548	89204	91865	94530	97194	99859	3
4	14548	15470	16425	17410	18427	19475	20553	21662	22802	23971	25169	26397	27653	28937	30249	31588	4
5	2732	2968	3218	3482	3761	4054	4362	4686	5026	5381	5753	6141	6546	6969	7408	7865	5
6	418	465	514	569	627	689	757	829	905	988	1077	1170	1270	1374	1487	1605	6
7	54	60	69	78	87	98	111	123	138	153	169	188	207	229	251	276	7
8	5	7	8	9	11	13	13	16	18	20	23	25	29	32	37	41	8
9	1	1	1	1	1	1	2	2	2	3	3	4	4	4	4	5	9
10														1	1	1	10

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0220

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	956484	935442	914862	894735	875051	855800	836972	818559	800550	782938	765714	748868	732392	716280	700522	685110	0
1	43032	63127	82318	100634	118104	134757	150620	165720	180083	193733	206695	218994	230652	241689	252130	261996	1
2	484	1420	2778	4528	6642	9094	11859	14911	18229	21790	25573	29557	33725	38058	42538	47148	2
3		11	42	102	200	341	534	783	1094	1471	1917	2438	3035	3710	4465	5303	3
4				1	3	8	15	26	43	66	97	137	187	250	327	417	4
5								1	1	2	4	6	9	13	17	25	5
6															1	1	6

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	670038	655297	640881	626781	612992	599506	586317	573418	560803	548465	536399	524598	513057	501770	490731	479935	0
1	271304	280076	288330	296087	303362	310174	316540	322474	327995	333117	337854	342223	346235	349904	353246	356270	1
2	51875	56702	61617	66605	71653	76751	81885	87049	92227	97415	102601	107776	112933	118067	123166	128228	2
3	6223	7228	8316	9488	10745	12085	13508	15012	16598	18261	20002	21819	23711	25673	27706	29807	3
4	525	651	795	961	1149	1359	1596	1858	2147	2465	2812	3190	3600	4043	4519	5029	4
5	33	44	58	73	93	117	143	175	212	254	304	359	421	491	569	656	5
6	2	2	3	5	6	8	10	13	17	21	26	32	40	48	58	68	6
7							1	1	1	2	2	3	3	4	5	7	7

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	469376	459050	448951	439074	429414	419967	410728	401692	392854	384212	375759	367493	359408	351501	343768	336204	0
1	358992	361420	363567	365445	367066	368438	369571	370476	371164	371640	371917	372001	371902	371627	371184	370582	1
2	133245	138211	143123	147973	152757	157471	162113	166677	171160	175561	179874	184099	188233	192274	196219	200069	2
3	31971	34200	36487	38834	41234	43688	46191	48742	51336	53972	56648	59358	62103	64877	67681	70508	3
4	5574	6154	6772	7425	8117	8845	9611	10416	11260	12141	13061	14021	15017	16054	17128	18240	4
5	752	859	975	1102	1241	1393	1557	1734	1925	2130	2351	2586	2838	3105	3390	3693	5
6	82	96	113	133	154	177	204	234	267	304	343	388	436	490	547	609	6
7	7	9	11	13	15	19	23	26	31	36	42	48	56	64	74	84	7
8	1	1	1	1	2	2	2	3	3	4	5	5	6	7	8	10	8
9												1	1	1	1	1	9

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	328808	321575	314500	307581	300814	294196	287724	281394	275203	269149	263227	257437	251773	246234	240816	235519	0
1	369826	368922	367881	366707	365406	363985	362449	360806	359059	357214	355277	353251	351143	348957	346698	344368	1
2	203819	207472	211024	214474	217824	221071	224215	227256	230194	233029	235761	238390	240918	243342	245666	247888	2
3	73359	76229	79116	82018	84932	87856	90787	93721	96660	99597	102533	105464	108388	111304	114209	117101	3
4	19390	20577	21801	23062	24359	25692	27059	28462	29897	31366	32867	34400	35963	37557	39179	40830	4
5	4012	4351	4708	5085	5480	5894	6331	6786	7263	7761	8281	8821	9384	9969	10575	11205	5
6	677	751	830	915	1007	1105	1210	1323	1443	1572	1707	1852	2006	2168	2340	2521	6
7	96	108	123	138	155	174	195	217	242	267	296	328	361	397	436	477	7
8	12	13	15	18	20	24	26	31	34	39	45	50	55	62	70	78	8
9	1	2	2	2	3	3	4	4	4	5	5	6	8	9	10	11	9
10									1	1	1	1	1	1	1	2	10

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0250

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17
0	950625	926860	903688	881096	859068	837592	816652	796236	776330	756921	737998	719548	701560	684021	666920	650247
1	48750	71296	92685	112960	132165	150336	167518	183746	199058	213491	227077	239850	251842	263084	273609	283441
2	625	1828	3566	5794	8472	11565	15034	18846	22969	27371	32024	36900	41973	47221	52616	58142
3		16	61	148	289	494	771	1127	1570	2105	2736	3469	4305	5247	6297	7454
4				2	6	13	24	44	71	108	158	222	304	403	524	669
5							1	1	2	4	7	11	15	23	32	45
6													1	1	2	2

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33
0	633991	618141	602688	587620	572930	558607	544641	531026	517750	504806	492186	479881	467885	456187	444783	433663
1	292611	301146	309070	316412	323192	329434	335165	340400	345166	349482	353364	356835	359910	362611	364949	366946
2	63775	69495	75287	81131	87012	92918	98830	104739	110631	116493	122319	128095	133813	139465	145044	150541
3	8721	10098	11582	13175	14874	16677	18583	20590	22693	24892	27181	29560	32024	34568	37191	39887
4	838	1035	1262	1520	1812	2139	2502	2904	3346	3829	4357	4927	5542	6205	6914	7671
5	60	80	104	132	167	208	257	312	378	452	536	631	740	859	993	1141
6	4	5	7	9	12	16	21	27	33	42	52	65	79	96	114	136
7				1	1	1	1	2	3	4	5	6	6	8	11	14
8													1	1	1	1

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49
0	422822	412251	401945	391896	382098	372546	363232	354151	345298	336665	328249	320043	312042	304240	296635	289218
1	368613	369968	371025	371799	372302	372546	372547	372314	371859	371196	370331	369279	368048	366649	365088	363378
2	155952	161269	166486	171599	176604	181497	186272	190930	195464	199874	204158	208312	212336	216229	219989	223617
3	42653	45486	48381	51333	54340	57396	60500	63643	66826	70041	73287	76559	79853	83165	86492	89828
4	8476	9330	10234	11188	12192	13246	14349	15503	16706	17960	19262	20612	22011	23457	24949	26489
5	1304	1484	1679	1894	2125	2377	2649	2942	3256	3591	3951	4334	4741	5172	5630	6112
6	162	190	223	258	300	346	396	452	515	584	658	741	830	928	1034	1149
7	16	20	25	30	35	41	49	58	68	79	92	106	122	140	159	181
8	2	2	2	3	4	5	5	6	7	9	11	12	15	18	21	25
9							1	1	1	1	1	2	2	2	3	3

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65
0	281988	274939	268065	261363	254829	248458	242247	236191	230286	224529	218916	213443	208106	202904	197831	192886
1	361523	359534	357420	355187	352841	350391	347842	345202	342477	339672	336793	333846	330837	327768	324647	321476
2	227111	230471	233697	236790	239750	242577	245273	247837	250271	252576	254754	256805	258731	260534	262215	263775
3	93174	96522	99871	103216	106556	109886	113203	116505	119788	123051	126288	129500	132683	135834	138951	142033
4	28071	29699	31370	33083	34836	36629	38460	40329	42233	44172	46144	48148	50181	52243	54333	56449
5	6622	7159	7722	8313	8932	9580	10256	10961	11696	12458	13252	14073	14926	15808	16718	17658
6	1274	1407	1551	1705	1870	2047	2235	2435	2649	2875	3114	3369	3635	3917	4216	4528
7	205	232	261	293	329	367	410	455	504	558	617	678	746	819	895	979
8	28	32	38	44	50	56	64	73	83	94	104	118	132	146	164	182
9	4	5	4	5	6	8	9	11	11	13	16	17	20	23	26	29
10			1	1	1	1	1	1	2	2	2	3	3	4	3	4
11															1	1

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0300

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	940900	912673	885293	858734	832972	807982	783743	760231	737424	715301	693842	673027	652836	633251	614254	595826	0
1	58200	84681	109521	132794	154572	174925	193916	211611	228069	243351	257509	270599	282672	293777	303960	313270	1
2	900	2619	5080	8214	11952	16230	20991	26178	31742	37631	43803	50214	56826	63601	70506	77509	2
3		27	105	254	492	837	1299	1890	2618	3491	4516	5694	7029	8523	10177	11986	3
4			1	4	12	26	50	87	142	216	314	440	598	791	1023	1298	4
5							1	3	5	10	15	25	37	54	75	104	5
6											1	1	2	3	5	7	6

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	577951	560613	543794	527480	511656	496307	481418	466975	452966	439376	426195	413410	401007	388977	377307	365988	0
1	321747	329432	336368	342591	348138	353042	357340	361063	364240	366902	369077	370789	372069	372936	373418	373535	1
2	84582	91698	98630	105956	113054	120107	127095	134002	140814	147518	154098	160548	166855	173012	179010	184842	2
3	13952	16070	18339	20754	23310	26003	28826	31774	34841	38020	41305	44689	48164	51725	55364	59073	3
4	1618	1988	2411	2889	3425	4021	4680	5405	6196	7055	7984	8984	10055	11199	12414	13702	4
5	140	185	238	303	381	473	579	702	843	1004	1186	1389	1617	1870	2150	2458	5
6	9	13	19	25	33	43	57	72	91	114	140	172	209	251	299	355	6
7	1	1	1	2	3	4	5	7	8	10	14	17	22	27	34	42	7
8									1	1	1	2	2	3	4	5	8

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	355009	344358	334027	324007	314287	304858	295712	286841	278236	269888	261791	253938	246320	238931	231763	224810	0
1	373308	372760	371908	370771	369367	367715	365830	363726	361419	358925	356254	353420	350435	347311	344059	340690	1
2	190502	195986	201290	206408	211339	216081	220629	224985	229148	233115	236890	240470	243859	247056	250064	252885	2
3	62846	66675	70555	74477	78436	82422	86432	90458	94493	98534	102571	106600	110617	114614	118588	122531	3
4	15064	16497	18002	19579	21226	22942	24727	26578	28495	30474	32516	34618	36777	38992	41261	43581	4
5	2796	3164	3564	3997	4464	4967	5506	6083	6697	7351	8045	8780	9554	10371	11229	12130	5
6	418	489	569	659	759	871	993	1129	1278	1440	1617	1810	2019	2246	2489	2752	6
7	51	63	76	90	108	126	150	174	203	236	272	312	357	407	462	523	7
8	5	6	8	11	13	16	19	23	27	33	39	45	54	62	74	84	8
9	1	1	1	1	1	2	2	3	4	4	4	6	7	9	10	12	9
10											1	1	1	1	1	2	10
11																	11

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0300

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	218065	211523	205178	199022	193052	187260	181642	176193	170907	165780	160806	155982	151303	146764	142361	138090	0
1	337215	333641	329976	326233	322416	318536	314598	310608	306577	302506	298405	294277	290127	285963	281786	277604	1
2	255518	257958	260240	262331	264248	265993	267569	268981	270229	271319	272255	273040	273677	274170	274524	274742	2
3	126442	130315	134143	137927	141659	145336	148957	152515	156008	159436	162792	166075	169285	172416	175470	178441	3
4	45949	48364	50823	53322	55861	58435	61041	63679	66344	69034	71746	74477	77225	79987	82760	85541	4
5	13075	14060	15090	16162	17276	18434	19634	20876	22160	23486	24852	26260	27706	29191	30715	32276	5
6	3032	3334	3655	3999	4364	4751	5161	5595	6054	6537	7046	7579	8140	8728	9341	9983	6
7	590	663	743	830	925	1028	1140	1261	1391	1531	1681	1842	2014	2198	2394	2602	7
8	98	113	129	148	168	191	216	244	275	307	344	385	428	476	527	583	8
9	14	17	20	22	27	31	36	41	47	54	62	70	80	89	102	115	9
10	2	2	3	4	3	4	5	6	7	9	9	11	13	15	17	19	10
11					1	1	1	1	1	1	2	2	2	3	3	4	11

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0350

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	931225	898632	867180	836829	807539	779276	752001	725681	700282	675772	652120	629296	607271	586016	565506	545713	0
1	67550	97779	125809	151756	175735	197847	218198	236881	253988	269609	283825	296715	308355	318818	328169	336476	1
2	1225	3546	6844	11008	15934	21527	27698	34365	41455	48893	56617	64570	72695	80943	89269	97630	2
3		43	165	400	771	1302	2009	2909	4009	5320	6846	8587	10546	12721	15109	17705	3
4			2	7	21	47	91	158	255	386	558	779	1052	1384	1781	2248	4
5						1	3	6	11	19	33	50	77	111	155	212	5
6										1	1	3	4	7	10	15	6
7															1	1	7

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	526613	508182	490395	473231	456668	440685	425261	410377	396014	382153	368778	355871	343415	331395	319797	308603	0
1	343799	350197	355727	360441	364389	367618	370175	372103	373442	374233	374510	374309	373664	372606	371163	369366	1
2	105990	114313	122570	130730	138769	146666	154400	161952	169308	176452	183374	190064	196513	202713	208659	214347	2
3	20503	23495	26673	30029	33554	37237	41066	45033	49125	53332	57641	62041	66522	71072	75679	80333	3
4	2788	3409	4111	4901	5781	6753	7820	8983	10245	11605	13066	14627	16286	18044	19900	21853	4
5	283	370	477	605	754	930	1135	1369	1635	1937	2275	2652	3072	3534	4042	4597	5
6	23	32	44	58	78	101	130	165	207	257	316	385	464	556	660	778	6
7	1	2	3	5	7	9	12	17	22	28	36	46	57	71	89	109	7
8						1	1	1	2	3	4	5	6	8	10	13	8
9													1	1	1	1	9

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	297803	287379	277321	267615	258249	249210	240488	232071	223948	216110	208546	201247	194203	187406	180847	174517	0
1	367238	364809	362099	359131	355928	352509	348893	345099	341143	337041	332809	328459	324008	319464	314842	310152	1
2	219772	224934	229829	234459	238822	242921	246757	250331	253649	256711	259522	262088	264410	266497	268350	269978	2
3	85025	89740	94472	99209	103943	108664	113363	118032	122662	127247	131778	136249	140653	144985	149238	153407	3
4	23899	26038	28268	30586	32987	35471	38032	40669	43377	46152	48990	51888	54841	57843	60894	63985	4
5	5201	5856	6562	7321	8136	9005	9932	10915	11956	13056	14215	15431	16707	18043	19435	20887	5
6	912	1062	1229	1417	1623	1851	2101	2376	2675	2999	3351	3732	4141	4580	5052	5555	6
7	132	159	192	227	269	317	371	431	498	575	660	754	859	973	1099	1238	7
8	16	21	25	31	38	46	55	66	80	94	111	130	151	177	205	236	8
9	2	2	3	4	4	5	7	9	11	13	16	19	23	28	33	39	9
10					1	1	1	1	1	2	2	3	4	3	4	5	10
11														1	1	1	11

PROBABILTY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0350

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	168409	162515	156827	151338	146041	140929	135997	131237	126644	122211	117934	113806	109823	105980	102270	98690	0
1	305405	300610	295777	290913	286028	281130	276222	271314	266412	261520	256644	251789	246959	242159	237394	232665	1
2	271384	272574	273555	274334	274914	275302	275506	275532	275383	275070	274595	273967	273192	272273	271218	270035	2
3	157486	161473	165362	169148	172830	176403	179865	183212	186443	189555	192549	195421	198169	200795	203297	205674	3
4	67116	70279	73470	76687	79923	83174	86437	89707	92980	96252	99517	102773	106016	109241	112446	115625	4
5	22395	23960	25581	27257	28987	30770	32605	34489	36421	38401	40426	42494	44603	46753	48940	51163	5
6	6092	6662	7268	7909	8586	9301	10051	10840	11669	12535	13440	14384	15369	16392	17454	18556	6
7	1389	1554	1733	1926	2135	2361	2604	2865	3144	3442	3760	4100	4459	4841	5246	5673	7
8	270	310	353	402	456	514	579	649	727	811	904	1003	1112	1229	1355	1492	8
9	46	54	63	72	84	97	111	129	146	167	189	215	242	273	306	342	9
10	7	8	9	12	14	16	20	22	26	30	35	40	47	53	61	70	10
11	1	1	2	2	2	3	3	3	4	5	6	7	8	9	11	13	11
12								1	1	1	1	1	1	2	2	2	12

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0400

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	921600	884736	849347	815372	782758	751447	721389	692534	664833	638239	612710	588201	564673	542086	520403	499587	0
1	76800	110592	141557	169870	195689	219173	240464	259700	277013	292527	306354	318610	329393	338805	346935	353874	1
2	1600	4608	8848	14156	20385	27396	35067	43283	51940	60943	70207	79652	89210	98817	108417	117958	2
3		64	245	590	1132	1903	2923	4209	5772	7618	9751	12169	14869	17842	21082	24574	3
4			3	12	35	79	152	263	420	634	914	1267	1704	2231	2854	3584	4
5					1	2	5	11	21	37	61	95	142	204	286	388	5
6									1	2	3	6	9	14	22	33	6
7														1	1	2	7
8																	8

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	479603	460419	442003	424322	407349	391055	375413	360397	345981	332142	318856	306102	293858	282103	270819	259986	0
1	359703	364498	368335	371283	373404	374762	375413	375413	374812	373659	371998	369872	367322	364384	361092	357482	1
2	127394	136688	145799	154701	163364	171766	179886	187707	195215	202399	209250	215760	221923	227739	233206	238320	2
3	28310	32273	36450	40823	45379	50098	54965	59961	65072	70277	75562	80909	86304	91729	97169	102611	3
4	4424	5379	6455	7654	8981	10437	12024	13742	15590	17569	19678	21913	24273	26754	29353	32066	4
5	516	672	860	1085	1348	1653	2004	2404	2858	3368	3935	4566	5259	6020	6849	7749	5
6	47	66	90	120	159	206	264	334	417	514	629	761	913	1087	1284	1507	6
7	3	5	7	11	15	21	28	38	50	65	82	104	131	161	199	242	7
8			1	1	1	2	3	4	5	6	9	12	15	21	26	33	8
9										1	1	1	2	2	3	4	9

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	249587	239604	230019	220819	211986	203506	195366	187552	180050	172847	165933	159296	152925	146808	140935	135298	0
1	353582	349421	345030	340428	335644	330698	325610	320400	315086	309686	304212	298681	293104	287497	281870	276232	1
2	243087	247507	251583	255322	258726	261803	264559	267000	269136	270974	272523	273790	274786	275519	275998	276233	2
3	108039	113441	118803	124114	129363	134537	139628	144626	149521	154305	158972	163514	167925	172199	176332	180319	3
4	34887	37814	40839	43957	47163	50452	53815	57247	60742	64294	67894	71537	75217	78925	82656	86403	4
5	8722	9768	10890	12089	13363	14715	16144	17651	19235	20895	22631	24442	26325	28281	30307	32401	5
6	1757	2035	2345	2686	3063	3474	3924	4413	4943	5514	6129	6789	7496	8249	9050	9900	6
7	292	352	419	496	583	683	794	920	1059	1214	1387	1576	1784	2013	2262	2534	7
8	41	51	63	77	94	113	137	162	193	228	267	312	363	419	484	554	8
9	5	6	8	11	13	17	20	25	30	37	45	54	64	76	89	105	9
10	1	1	1	1	2	2	3	4	4	5	6	8	9	12	15	18	10
11									1	1	1	1	2	2	2	3	11

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0400

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	129886	124690	119703	114915	110318	105906	101669	97602	93699	89951	86352	82898	79582	76399	73343	70410	0
1	270595	264968	259356	253769	248216	242699	237228	231806	226437	221127	215881	210700	205588	200547	195582	190691	1
2	276233	276007	275565	274918	274071	273037	271824	270440	268894	267196	265354	263375	261268	259041	256700	254256	2
3	184155	187838	191365	194733	197941	200986	203867	206586	209140	211531	213756	215820	217722	219465	221048	222474	3
4	90160	93919	97676	101423	105155	108867	112552	116204	119820	123392	126919	130392	133809	137165	140458	143681	4
5	34561	36785	39071	41415	43815	46269	48773	51324	53919	56555	59228	61936	64675	67440	70228	73038	5
6	10800	11751	12752	13805	14910	16065	17274	18534	19845	21209	22622	24086	25600	27163	28774	30432	6
7	2829	3148	3492	3862	4259	4686	5141	5626	6143	6690	7272	7886	8533	9216	9934	10688	7
8	633	721	818	925	1043	1171	1312	1465	1632	1812	2007	2217	2445	2688	2949	3229	8
9	123	143	167	193	222	255	291	332	377	428	483	545	611	684	765	851	9
10	21	25	29	35	42	49	57	67	77	89	103	118	135	154	175	199	10
11	3	4	5	6	7	9	10	12	14	17	19	22	26	31	36	42	11
12	1	1	1	1	1	1	2	2	3	3	3	4	5	6	7	7	12
13											1	1	1	1	1	2	13
14																	14

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0500

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	902500	857375	814506	773781	735092	698337	663420	630249	598737	568800	540360	513342	487675	463291	440127	418121	0
1	95000	135375	171475	203626	232134	257283	279335	298540	315125	329305	341280	351235	359339	365757	370632	374107	1
2	2500	7125	13538	21435	30544	40623	51457	62850	74634	86660	98792	110915	122932	134752	146303	157519	2
3		125	475	1128	2144	3563	5416	7718	10476	13683	17331	21405	25881	30733	35934	41452	3
4			6	30	84	188	357	610	964	1440	2053	2816	3746	4852	6147	7637	4
5					2	6	15	32	61	106	173	267	394	562	776	1044	5
6								1	3	6	11	19	31	49	75	110	6
7												1	2	4	6	9	7
8																1	8

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	397214	377354	358486	340561	323534	307357	291989	277389	263520	250344	237827	225936	214639	203906	193712	184026	0
1	376309	377353	377354	376411	374617	372063	368828	364987	360606	355752	350482	344848	338903	332691	326250	319624	1
2	168348	178747	188676	198111	207026	215406	223239	230518	237242	243410	249026	254100	258637	262650	266152	269157	2
3	47256	53310	59582	66036	72640	79359	86161	93016	99891	106758	113591	120363	127049	133629	140081	146383	3
4	9326	11223	13328	15641	18161	20885	23808	26925	30230	33713	37366	41176	45137	49232	53451	57783	4
5	1375	1772	2245	2798	3441	4177	5013	5952	7000	8162	9439	10836	12352	13992	15754	17640	5
6	157	218	295	393	513	659	835	1044	1290	1576	1905	2282	2710	3191	3731	4332	6
7	14	21	31	45	61	84	113	149	194	248	315	394	488	600	730	879	7
8	1	2	3	4	6	9	13	18	24	33	43	57	74	95	120	151	8
9					1	1	1	2	3	4	5	7	10	12	17	22	9
10											1	1	1	2	2	3	10

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	174825	166083	157779	149890	142396	135276	128512	122087	115982	110183	104674	99440	94468	89745	85258	80994	0
1	312844	305944	298951	291892	284792	277672	270552	263449	256382	249362	242402	235517	228713	222000	215387	208882	1
2	271680	273738	275348	276529	277296	277671	277672	277316	276623	275610	274299	272703	270844	268737	266401	263850	2
3	152522	158480	164243	169798	175135	180243	185114	189742	194120	198246	202114	205723	209072	212162	214989	217560	3
4	62213	66729	71316	75963	80654	85378	90122	94872	99615	104340	109035	113690	118291	122830	127297	131681	4
5	19647	21774	24023	26386	28866	31456	34151	36950	39846	42835	45910	49065	52297	55596	58959	62376	5
6	4997	5731	6532	7407	8356	9381	10485	11668	12933	14278	15706	17217	18809	20483	22238	24074	6
7	1052	1249	1473	1727	2011	2328	2681	3070	3500	3972	4487	5048	5657	6315	7023	7784	7
8	187	230	281	340	409	490	581	687	806	941	1093	1262	1451	1661	1894	2151	8
9	29	36	47	58	72	89	109	133	160	192	230	273	322	379	443	515	9
10	4	5	6	9	11	14	18	22	28	35	42	52	63	76	91	109	10
11		1	1	1	2	2	3	4	4	5	7	8	11	14	17	20	11
12									1	1	1	2	2	2	3	3	12
13																1	13

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0500

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	76945	73098	69443	65971	62673	59539	56562	53734	51047	48495	46070	43766	41578	39499	37524	35648	0
1	202487	196209	190054	184023	178120	172348	166707	161200	155828	150588	145483	140513	135676	130971	126398	121953	1
2	261101	258171	255073	251822	248432	244916	241289	237559	233740	229845	225883	221862	217794	213689	209552	205396	2
3	219875	221936	223747	225314	226639	227729	228588	229223	229640	229846	229845	229648	229258	228685	227935	227015	3
4	135975	140170	144259	148233	152087	155815	159410	162870	166187	169359	172384	175257	177977	180540	182948	185198	4
5	65841	69348	72889	76457	80046	83648	87256	90864	94464	98050	101616	105154	108659	112125	115546	118916	5
6	25990	27982	30050	32192	34406	36687	39036	41446	43918	46445	49025	51655	54330	57046	59800	62587	6
7	8598	9468	10393	11377	12417	13517	14675	15894	17171	18508	19905	21361	22876	24449	26078	27764	7
8	2432	2740	3077	3442	3839	4268	4731	5227	5761	6332	6940	7588	8277	9007	9780	10595	8
9	597	690	792	907	1033	1173	1328	1499	1685	1888	2111	2352	2614	2897	3202	3531	9
10	129	152	179	209	245	285	328	378	434	497	566	644	729	823	927	1041	10
11	25	30	36	44	51	61	73	85	100	117	136	157	181	209	240	274	11
12	4	5	7	7	10	12	14	17	20	24	29	35	41	48	56	65	12
13	1	1	1	2	2	2	3	3	4	5	6	7	8	10	11	14	13
14								1	1	1	1	1	2	2	3	2	14
15																1	15

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0600

n	m = 2	m = 3	m = 4	m = 5	m = 6	m = 7	m = 8	m = 9	m = 10	m = 11	m = 12	m = 13	m = 14	m = 15	m = 16	m = 17	n
0	883600	830584	780749	733904	689870	648478	609569	572994	538615	506299	475920	447365	420523	395292	371574	349280	0
1	112800	159048	199340	234225	264205	289745	311269	329168	343797	355485	364535	371218	375787	378470	379481	379005	1
2	3600	10152	19086	29901	42160	55483	69539	84043	98750	113453	127975	142168	155911	169105	181665	193536	2
3		216	812	1908	3589	5902	8877	12517	16809	21725	27229	33274	39808	46773	54114	61766	3
4			13	61	172	377	709	1198	1877	2774	3910	5310	6987	8957	11225	13799	4
5				1	4	15	36	77	144	247	400	610	892	1258	1720	2290	5
6							1	3	8	16	29	51	85	133	201	292	6
7										1	2	4	7	11	19	29	7
8														1	1	3	8

n	m = 18	m = 19	m = 20	m = 21	m = 22	m = 23	m = 24	m = 25	m = 26	m = 27	m = 28	m = 29	m = 30	m = 31	m = 32	m = 33	n
0	328323	308623	290106	272700	256338	240957	226500	212910	200136	188128	176839	166229	156256	146880	138067	129783	0
1	377222	374289	370349	365534	359963	353747	346979	339750	332139	324219	316055	307702	299212	290636	282011	273374	1
2	204663	215017	224573	233319	241253	248375	254698	260235	265006	269033	272344	274966	276931	278267	279009	279190	2
3	69673	77772	86006	94321	102661	110976	119220	127348	135321	143103	150659	157960	164981	171698	178092	184147	3
4	16677	19856	23332	27092	31125	35418	39951	44708	49666	54806	60103	65537	71082	76716	82415	88155	4
5	2980	3803	4765	5880	7153	8590	10200	11985	13949	16091	18415	20916	23593	26442	29459	32636	5
6	413	566	761	1000	1293	1646	2062	2550	3116	3766	4505	5340	6274	7314	8461	9722	6
7	45	67	97	137	189	255	338	442	568	721	904	1120	1374	1667	2006	2393	7
8	4	6	10	15	23	32	46	64	87	115	152	196	252	320	400	496	8
9		1	1	2	2	4	5	7	11	16	21	30	39	52	69	88	9
10							1	1	1	2	3	4	5	7	10	14	10
11													1	1	1	2	11

n	m = 34	m = 35	m = 36	m = 37	m = 38	m = 39	m = 40	m = 41	m = 42	m = 43	m = 44	m = 45	m = 46	m = 47	m = 48	m = 49	n
0	121996	114676	107796	101328	95249	89534	84162	79112	74365	69903	65709	61767	58061	54577	51302	48224	0
1	264759	256193	247702	239308	231028	222881	214880	207037	199362	191863	184545	177414	170475	163731	157182	150829	1
2	278840	277996	276687	274948	272810	270304	267459	264304	260868	257176	253258	249136	244832	240370	235772	231057	2
3	189850	195189	200158	204749	208961	212791	216242	219315	222014	224347	226316	227932	229205	230143	230756	231057	3
4	93915	99671	105401	111087	116707	122243	127675	132989	138169	143199	148068	152763	157273	161589	165703	169605	4
5	35967	39444	43058	46799	50656	54618	58676	62817	67027	71295	75610	79957	84326	88702	93075	97433	5
6	11096	12589	14200	15931	17783	19756	21848	24057	26383	28822	31370	34024	36780	39633	42577	45607	6
7	2833	3328	3885	4504	5189	5945	6773	7678	8660	9724	10869	12100	13415	14817	16306	17883	7
8	611	744	898	1077	1284	1518	1784	2083	2419	2793	3209	3669	4174	4729	5334	5992	8
9	112	142	179	222	273	334	404	487	583	693	819	962	1125	1308	1514	1743	9
10	18	24	31	40	50	63	81	100	123	150	183	221	266	317	376	445	10
11	3	4	4	6	9	11	13	18	23	29	36	45	56	68	83	100	11
12			1	1	1	2	3	3	3	5	7	8	10	13	17	21	12
13									1	1	1	2	2	3	2	3	13
14															1	1	14

PROBABILITY IN MILLIONTHS THAT n UNITS WOULD BE OUT SIMULTANEOUSLY
FROM GROUPS OF m UNITS WHEN THE OUTAGE RATE IS p = 0.0600

n	m = 50	m = 51	m = 52	m = 53	m = 54	m = 55	m = 56	m = 57	m = 58	m = 59	m = 60	m = 61	m = 62	m = 63	m = 64	m = 65	n
0	45331	42611	40055	37651	35392	33268	31272	29396	27632	25975	24416	22951	21574	20280	19063	17919	0
1	144672	138712	132945	127374	121989	116794	111782	106951	102298	97817	93507	89362	85377	81548	77872	74344	1
2	226244	221349	216391	211385	206343	201282	196213	191147	186095	181068	176073	171118	166213	161363	156575	151852	2
3	231056	230768	230203	229374	228295	226977	225436	223683	221731	219592	217280	214808	212187	209429	206544	203547	3
4	173293	176758	179999	183011	185793	188344	190661	192747	194604	196231	197633	198812	199772	200517	201051	201380	4
5	101763	106056	110297	114479	118592	122623	126567	130413	134152	137780	141287	144668	147916	151027	153997	156821	5
6	48717	51899	55149	58458	61819	65225	68669	72143	75639	79150	82668	86185	89694	93187	96658	100097	6
7	19546	21296	23132	25053	27057	29144	31308	33549	35865	38252	40706	43223	45801	48435	51120	53853	7
8	6706	7476	8306	9195	10147	11161	12240	13385	14595	15870	17213	18623	20099	21641	23248	24920	8
9	1997	2280	2591	2935	3310	3720	4167	4651	5175	5740	6348	7000	7697	8441	9234	10075	9
10	523	611	712	824	951	1093	1250	1425	1619	1833	2067	2324	2604	2910	3241	3601	10
11	121	146	173	206	242	285	334	389	450	520	599	687	786	895	1016	1149	11
12	25	31	38	46	56	67	80	95	113	133	156	183	213	247	286	330	12
13	5	6	7	9	11	14	17	21	26	31	37	44	52	62	74	86	13
14	1	1	2	2	3	2	3	4	5	6	8	10	12	14	17	20	14
15						1	1	1	1	2	2	2	2	3	3	5	15
16													1	1	1	1	16

No Discussion

Forty-Two Years' Experience Combating Sleet Accumulations

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SLEET has been of much concern to the Pennsylvania Water and Power Company since the first transmission line was placed in service in 1910. This paper describes basic methods used to avoid circuit outages and damage from sleet. The historical development of the methods is outlined along with the reasons for the several steps therein.

The word "sleet" is used to cover all the weather phenomena which may cause deposits on transmission lines such as ice, glaze, wet snow, freezing rain, and sleet.

Damage caused by sleet may be electrical or mechanical, or both. The factors involved are unequal loading and unloading of conductors, dancing conductors, and mechanical failure.

The Transmission System

The high-voltage transmission system owned and operated by Pennsylvania Water and Power Company is used for bulk power transmission and regional interconnections as indicated in Figure 1. Power generated at the Holtwood and Safe Harbor Hydro Stations and at the Holtwood Steam Station is delivered to other power companies and to the Pennsylvania Railroad.

About one-half of the approximately 600 circuit miles which comprise this system is located in Pennsylvania and the other half in Maryland. Sleet storms are prevalent over the area. The average daily mean temperature from November 15 to March 31 is 36 degrees Fahrenheit. Sleet-melting operations are required on 400 circuit miles of this system. All lines are constructed on steel towers and the circuits operate at 66, 132, and 220 kv.

Historical Development

As experience was gained and as the system expanded, methods were developed to combat sleet, as described here.

Double-Circuit 66-Kv 25-Cycle Lines

The first line was put in service in October 1910 between the Holtwood Hydro Station and Baltimore. This is a double-circuit 66-kv 25-cycle line using 300,000-circular-mil all-aluminum conductor spaced 7 feet apart in a vertical plane, as shown in Figure 2. The first sleet trouble occurred only 1 month later during a heavy wet snowstorm. Tests indicated that both circuits were short-circuited. However, when the storm cleared they were returned to service without repairs.

Some interesting mechanical tests[1] were carried out in 1913. A conductor on one span of seven between strain towers was loaded with weights to simulate 1/2-inch ice loading, and the change in sag and the deflection of intermediate suspension insulators between the strain points were measured. The sag increased 15 feet when the third span from a strain tower was so loaded. It can be seen readily that with vertical configuration such unequal loading on a top or middle conductor would result in contact with an unloaded lower conductor. Cases were observed at a later date where frozen snow had accumulated unevenly on the conductors causing the middle conductor to sag below the lower conductor.

Also, tests were made in 1913 and 1914 on 300,000-circular-mil all-aluminum conductor which determined that a current of 300 amperes would prevent accumulation of sleet effectively on this cable when the temperature was not lower than -3 degrees centigrade, and wind velocity was approximately 2 miles per hour. With lower temperatures and higher wind velocities ice did accumulate, although at a much slower rate than on idle unheated conductors.

Sleet trouble again occurred in February 1914. By this time, the Holtwood hydrogenerating capacity had increased and efforts were made to prevent sleet accumulation on the line conductors by loading the circuits with commercial load and by operating with customer synchronous equipment underexcited and the Holtwood generators overexcited.

In August 1914, one circuit of the second line on the same right-of-way was placed in service. As a result of the mechanical tests already referred to, and observations during actual sleet storms, the vertical spacing of the conductors was increased from 7 to 9 feet, and the lengths of the crossarms varied to obtain horizontal offsets of 4 feet between the top and middle conductors and 3 feet between the middle and bottom conductors. This is shown in Figure 3. More strain towers also were used, the maximum number of spans between such towers being five.

After sleet troubles which involved all three circuits to Baltimore in December 1914, methods were developed to heat the circuits to prevent sleet formation, depending upon system load conditions. Under light load conditions, the load was carried on one circuit and the other two circuits removed from service, connected in series and heated, as indicated in Figure 7.

If the load became too great for one circuit, all three were placed in service and circulating current provided as indicated in Figure 8. Five overexcited hydrogenerators operating in parallel were connected to two circuits through their respective transformers. Two separate hydrogenerators with open field circuits were connected to the third circuit. At the Baltimore substation the transformers were connected to the 13-kv load bus, but were not paralleled on the 66-kv side. Sufficient reactive kilovars circulated through the line to the generators with open field to prevent accumulation of sleet.

To obtain higher values of heating current, the next step in the development was to operate with two circuits loaded and the third circuit on heat run, as indicated in Figure 9. The circuit to be heated was short-circuited and grounded at the Baltimore Substation and connected by jumpers to the 11-kv side of a transformer at Holtwood. The transformer then was energized from the 66-kv bus. About 350 amperes was obtained by this method.

As a further improvement, a sleet bus was installed at Holtwood, as indicated in Figure 10. Disconnectors were installed to connect the circuits to the sleet bus and the sleet bus was supplied through an oil circuit breaker from the main 11-kv bus. The circuit, being heated, thus was connected directly to the 11-kv bus, and the heating current was increased to 390 amperes. The sleet bus feeder was equipped with overcurrent re-

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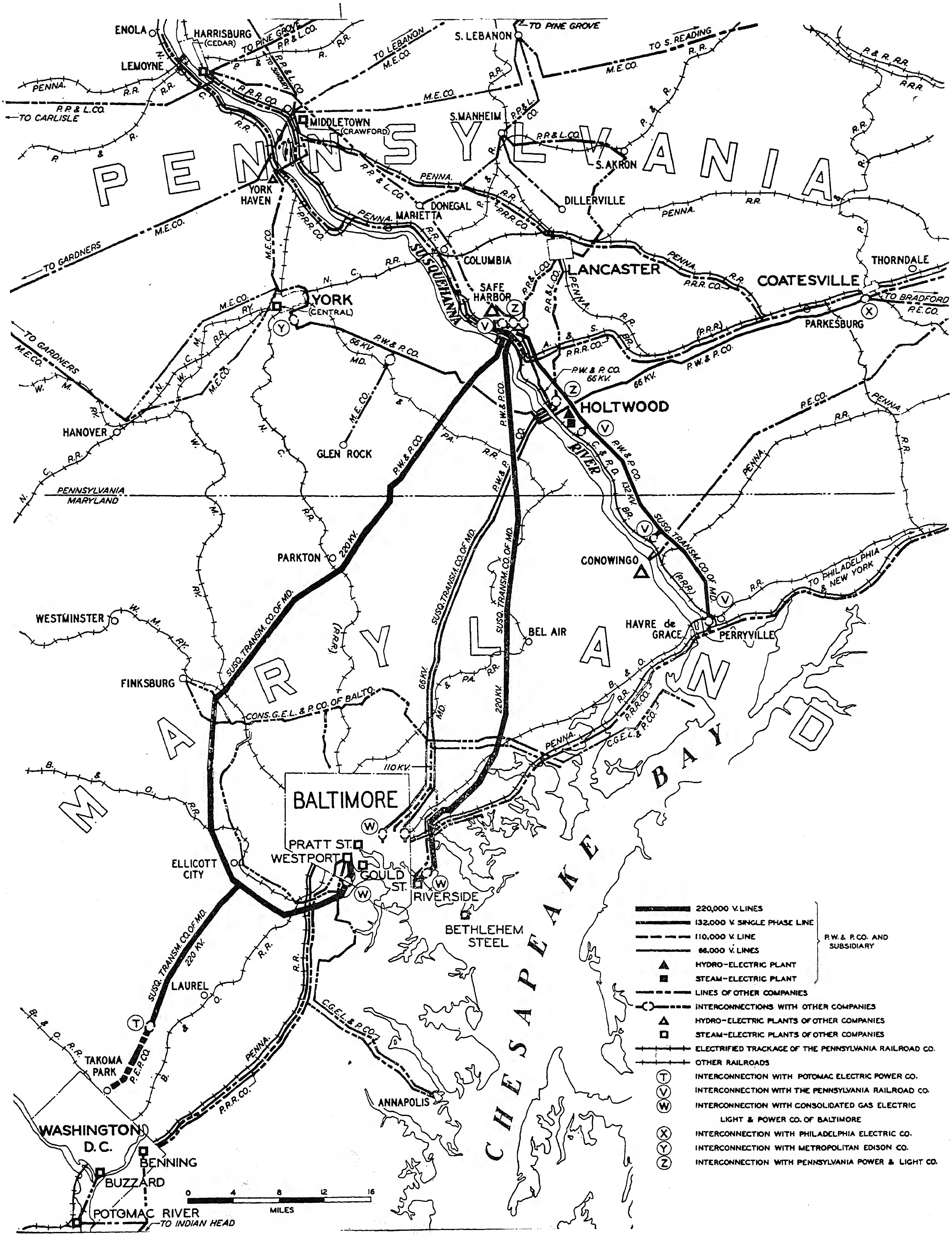

Figure 1. The regional transmission network of the Pennsylvania Water and Power Company and interconnections

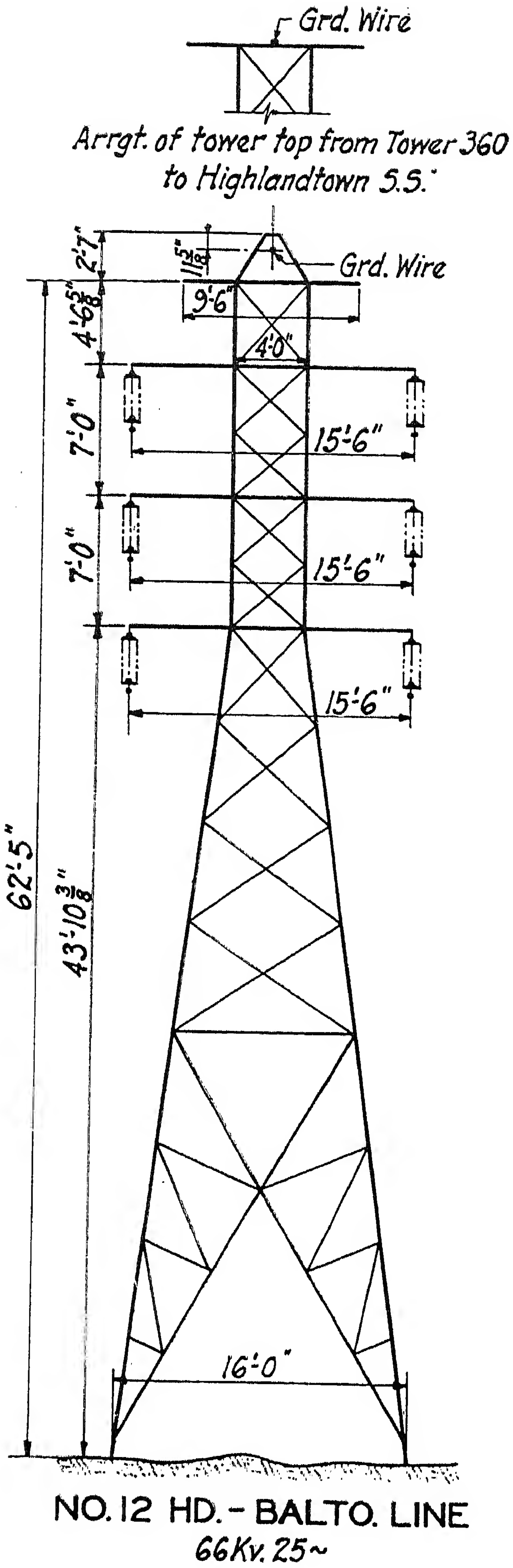

Figure 2. Double-circuit 66-kv 25-cycle 1910 line. 300,000-circular-mil all-aluminum conductor

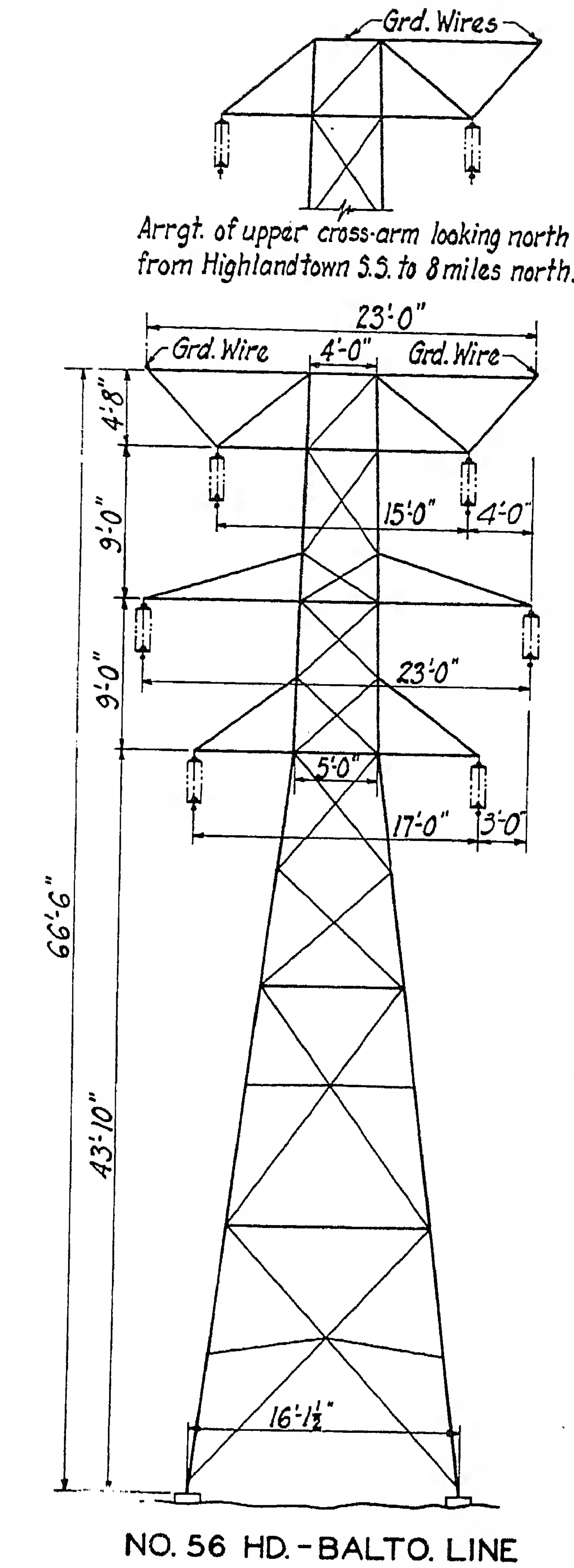

Figure 3. Double-circuit 66-kv 25-cycle 1914 line. 300,000-circular-mil all-aluminum conductor

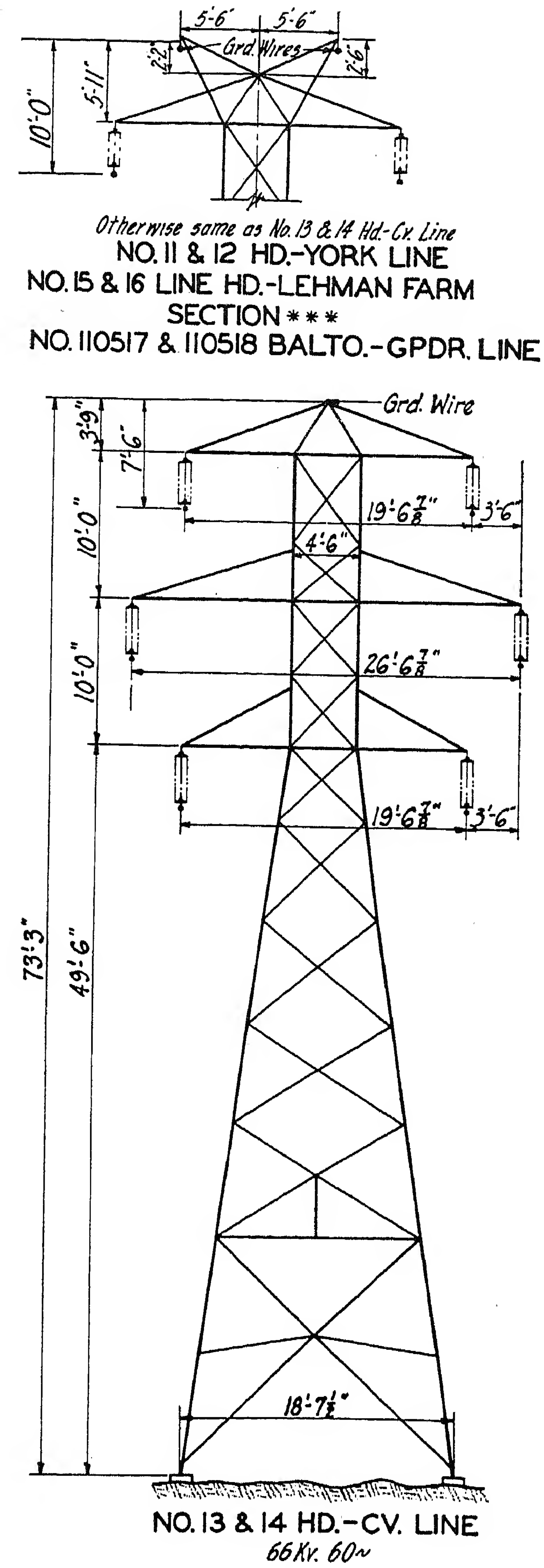

Figure 4. Double-circuit 66-kv 60-cycle 1923 and 1925 lines. 2/0 copper conductor

lays to remove faulted circuits from the system. The bus greatly facilitated the application of the heating current.

The fourth Baltimore circuit was completed on the second tower line in November 1917, using the same construction used for the third circuit, experience having shown this to be less susceptible to sleet trouble than the original line. This circuit also was provided with disconnectors to connect it to the sleet bus. The same method was followed for heating four circuits as for three, except that while two circuits were loaded and the third heated the fourth was left idle. After being on heat run, the circuit was returned to service, one of the loaded circuits removed from service to remain idle, and the former idle circuit heated next.

Efforts to load the circuits in service with commercial load and reactive kilovars were abandoned in 1920 because increased 25-cycle system load limited the availability of kilovars for this purpose.

Recognizing the possibility of interruption of communications between stations during sleet storms, a schedule was established in 1920 for applying heating current in a regular sequence without telephone communication. This was so successful that the schedules have been continued, although alternate and reliable communications are now available.

These schedules and the sleet bus speeded up the application of the heating current and, together with the higher and more effective current, made it possible to melt the sleet from the conductor rather than merely prevent it from forming.

These procedures were successful on the four 25-cycle 66-kv circuits until 1932 when failures occurred on three of them, and several automatic relay operations occurred on the fourth. Since this experience, a separate generator operating at a higher voltage has been used to supply a heating current of 425 amperes. Current is increased further to 450 amperes whenever difficulty in melting sleet is experienced because of low temperature

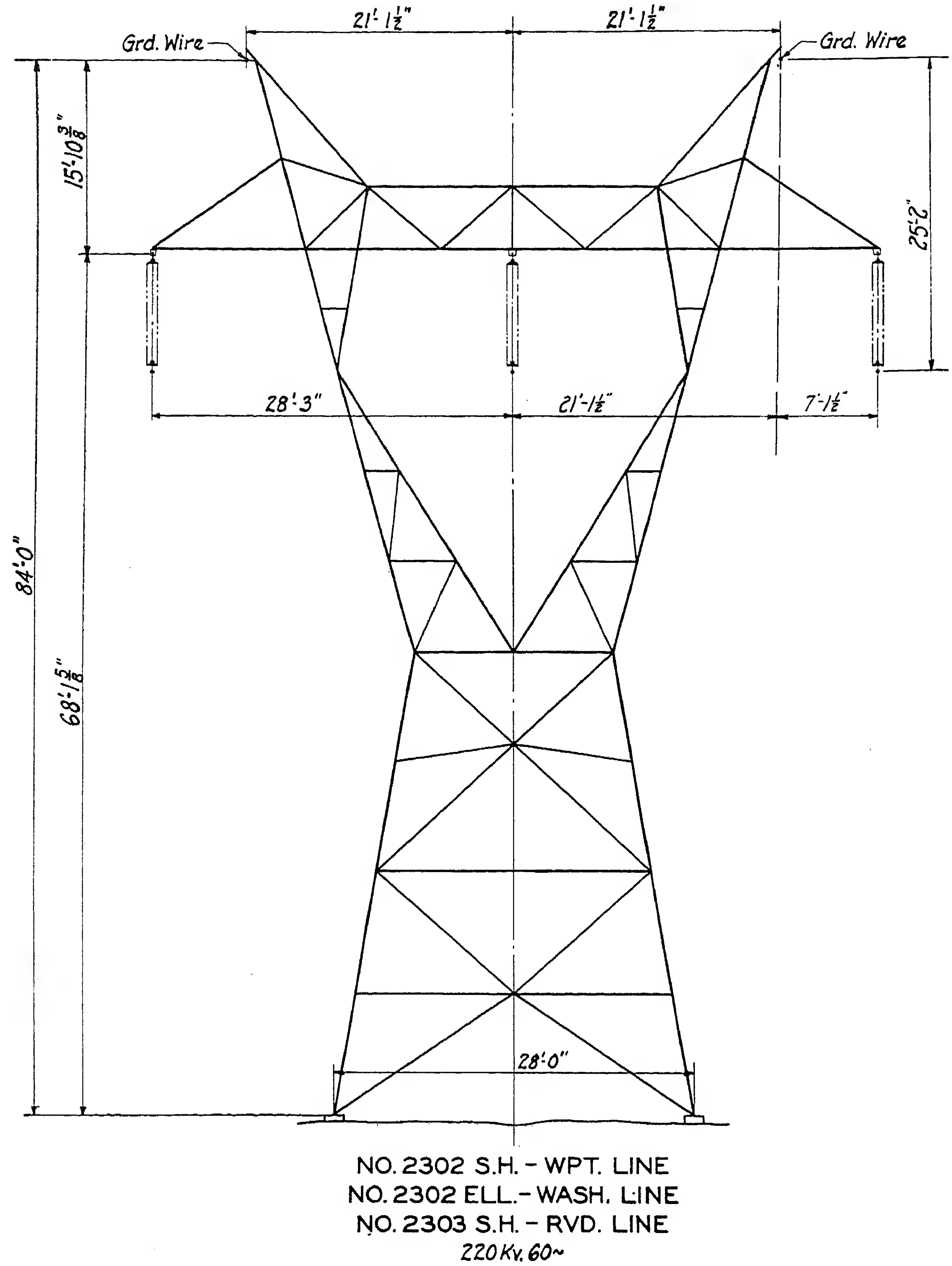

Figure 5. Single-circuit 220-kv 60-cycle 1931 and 1937 lines. 795,000-circular-mil steel-reinforced aluminum-cable conductor

or high wind velocity. The sleet bus feeder relay protection was increased by the use of improved type overcurrent relays and by the addition of a current balance relay.

Since 1932 there have been four occasions when circuit failures occurred. In each case, the circuit failed before heat runs were started. Three of these failures occurred during storms when conductors were reported clear of sleet at points under observation. The fourth occurred during an unusual wind and snow storm of such short duration that heat runs could not have been started. It is interesting to note that following one of these storms a small deposit of frozen snow and ice was left unevenly on portions of two of the circuits and could not be melted with 450 amperes because of low temperature and high wind velocity. Since the generator was operating near the maximum voltage limit, higher current was obtained by short-circuiting the circuit on a line tower and heating only part of it, including the portions on which the sleet was present.

Double-Circuit 66-Kv 60-Cycle Lines

In 1923, double-circuit 66-kv 60-cycle lines were built from Holtwood to York and from Holtwood to Coatesville, and in 1925 from Holtwood to Lancaster. These lines use 2/0 copper conductors with a vertical spacing of 10 feet, and with the middle crossarm longer than the top and bottom arms, as indicated in Figure 4, to provide an offset of $3^1/_2$ feet.

Sleet-melting facilities on the first two of these lines initially consisted of a sleet bus with associated disconnectors on each circuit. Heating current was supplied by a 60-cycle generator and a value of 340 amperes was used. Trouble developed on these lines during a severe sleet storm in February 1927, with minimum temperatures in some locations of −3.5 degrees centigrade. The circuits tripped while being heated at 370 amperes and also immediately after being returned to service following a heat run. Observations made during this storm verified that the current being used was insufficient to remove the sleet accumulation effectively at all points along the line in the normal 40-minute heating period during a storm of this nature. The solution was to increase the current or the time. Since there were four circuits to be heated from one sleet bus, increasing the time was undesirable as this would allow an increased accumulation of sleet on the three circuits in service with added possibility of failure. Furthermore, due to the length of the lines, the 60-cycle generator used to supply the heating current was operating up to its maximum voltage limit. Taking advantage of the lower line reactance at 25 cycles, facilities were provided in the fall of 1927 whereby a separate 25-cycle generator could be connected to the sleet bus to provide higher heating current. A value of 400 amperes was used then.

Initially the sleet bus was equipped with overcurrent relays. A current balance relay was added later.

Trouble was again experienced on one of these lines in March 1932 during the storm previously mentioned when discussing the failures of the Baltimore circuits. The phenomenon of dancing conductors was observed at several locations and was undoubtedly a contributing factor to the failure. Since that time, the heating current has been increased to 450 amperes for 2/0 copper conductors. When difficulty is encountered in melting sleet due to low temperature or high wind velocity, the heating current is further increased to approximately 500 amperes, the limit being dictated by the voltage limitation on the generating equipment used to supply the heating current.

These methods have been successful to date. There has been only one case of sleet trouble since 1932 and this occurred before the heat runs were started.

No conductor failures have occurred on the Lancaster line. However, during the March 1932 storm, one of the circuits relayed three times within 4 minutes due to dancing conductors. Later investigation revealed that three short circuits between top and middle conductors had

occurred in one span. High-speed directional distance relays prevented a burndown.

A 10-mile-long line utilizing the same design, except with shorter spans and with practically all spans in strain, was placed in service in March 1925. No sleet trouble has been experienced, although the conductors are not heated during sleet storms. It is of interest to note that this line is on the right-of-way with the Baltimore end of the two 66-kv 25-cycle lines shown in Figures 2 and 3, and crosses known trouble spots where the older lines have since failed.

When the Safe Harbor Hydro Station was constructed, the Holtwood-Lancaster 2/0 conductor circuits were tapped nearby and extended to Safe Harbor with 4/0 conductor. Since sleet-melting facilities were not provided for this 4/0 section of line, only strain towers were used. Otherwise, the design is similar to that shown in Figure 4. No sleet trouble has been experienced on this line during its 20 years' operation.

Single-Circuit 220-Kv 60-Cycle Lines

With the construction of the Safe Harbor Hydro Station, the first 220-kv line was built to Baltimore in 1931, and tapped and extended to Washington in 1933. The tower used is shown in Figure 5. The conductor is 795,000-circular-mil steel-reinforced aluminum cable. The second line to Baltimore was built in 1937 using the same design. The basic structural design is for 1/2 inch ice, 8 pounds wind, 0 degrees centigrade, with another design requirement of 1 inch ice and no wind. No facilities are provided to melt sleet and to date no sleet trouble has been experienced on these lines.

A new 220-kv line is under construction from Safe Harbor to the Harrisburg area which will be of the same general design.

Four-Circuit 132-Kv 25-Cycle Single-Phase Line

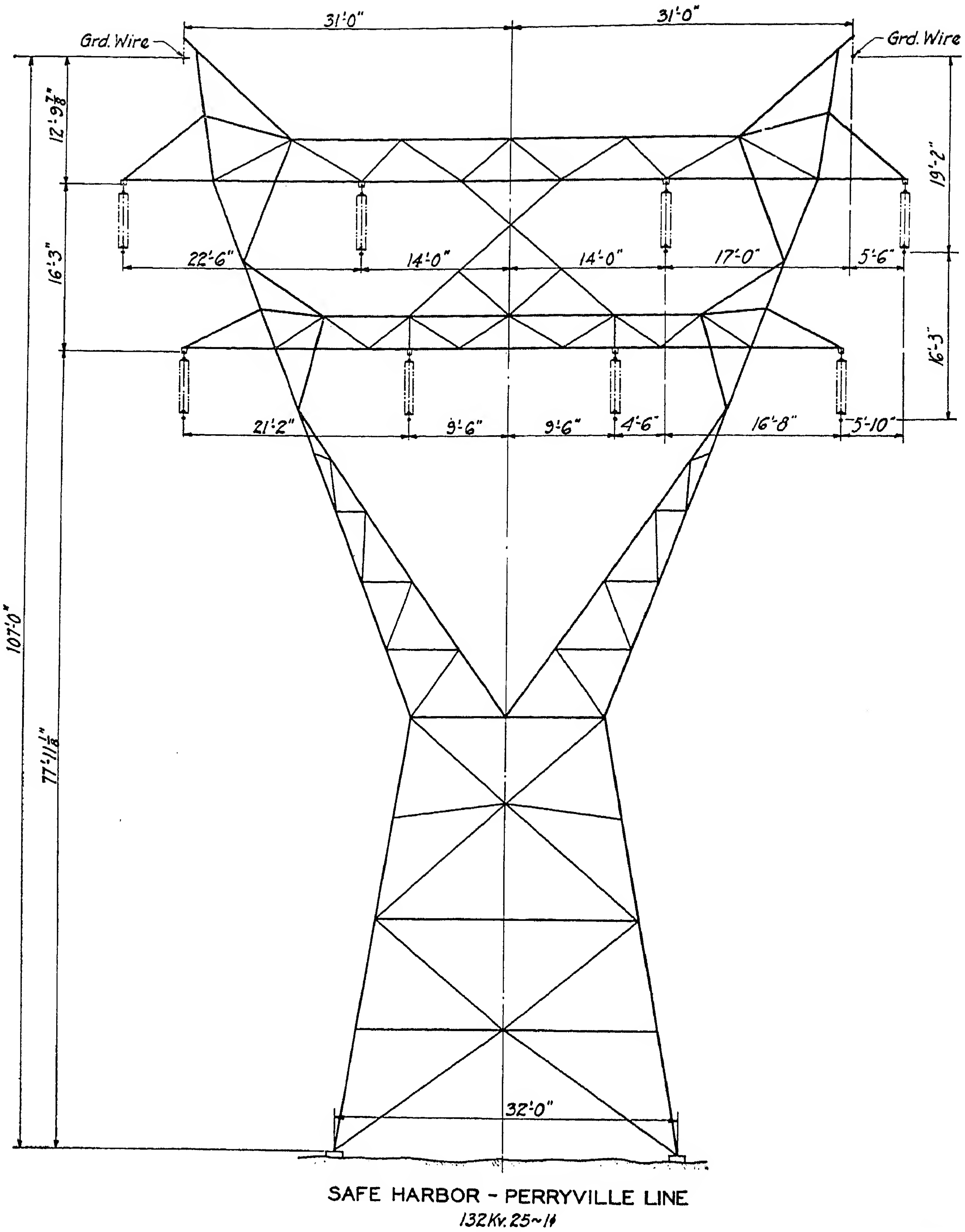

Figure 6. Four-circuit 132-kv 25-cycle 1934 line. 397,500-circular-mil steel-reinforced aluminum-cable conductor

In October, 1934, a line with four single-phase 132-kv circuits was placed in service from Safe Harbor to Perryville to supply the Pennsylvania Railroad. Figure 6 shows the tower arrangement. The conductors are 397,500-circular-mil steel-reinforced aluminum cable. The two conductors of the same circuit have a vertical separation of 16 feet 3 inches and a horizontal offset of 5 feet 10 inches for the two outside circuits and $4^1/_2$ feet for the two inside circuits. Initially, no sleet-melting facilities were provided for these circuits.

During a wet snow storm in February 1936, trouble was experienced from dancing conductors. Relay action occurred on three of the four circuits. One circuit tested bad and was found with both conductors burned down. Burn marks were found on both conductors of one of the other circuits.

After this experience, sleet-melting facilities were provided for these circuits. These facilities include a relay-protected sleet bus supplied through an automatic oil circuit breaker, and gang-operated sleet disconnectors for each circuit. Normally, two circuits are heated simultaneously with 600 amperes per circuit for 60 minutes. No further trouble has occurred.

Short double-circuit taps to these circuits were built in 1937, using horizontal conductor configuration construction similar to that indicated in Figure 6, except that the four top conductors are omitted. No facilities are provided to melt sleet on these taps, and no trouble has been experienced.

Preventive Maintenance and Repairs

A maintenance organization and program have been developed with particular emphasis on the prevention of trouble and the prompt restoration of service in the event of circuit failure. This development has been greatly influenced by sleet. Since there is no distribution system, there is a comparatively small force of linemen. However, they are highly skilled in the location and repair of both minor and major damage to steel tower transmission lines. Rather than operate from a centralized headquarters, it is advantageous to continue the original practice of having these men scattered along the lines, chiefly because of sleet. Each

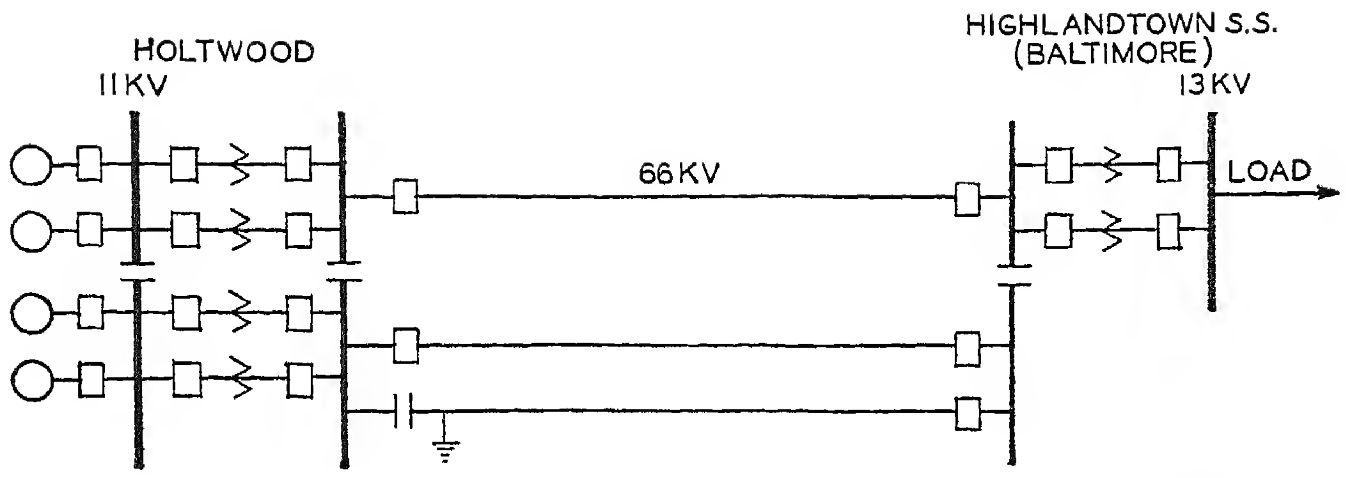

Figure 7. Method of melting sleet during light load periods, 1915

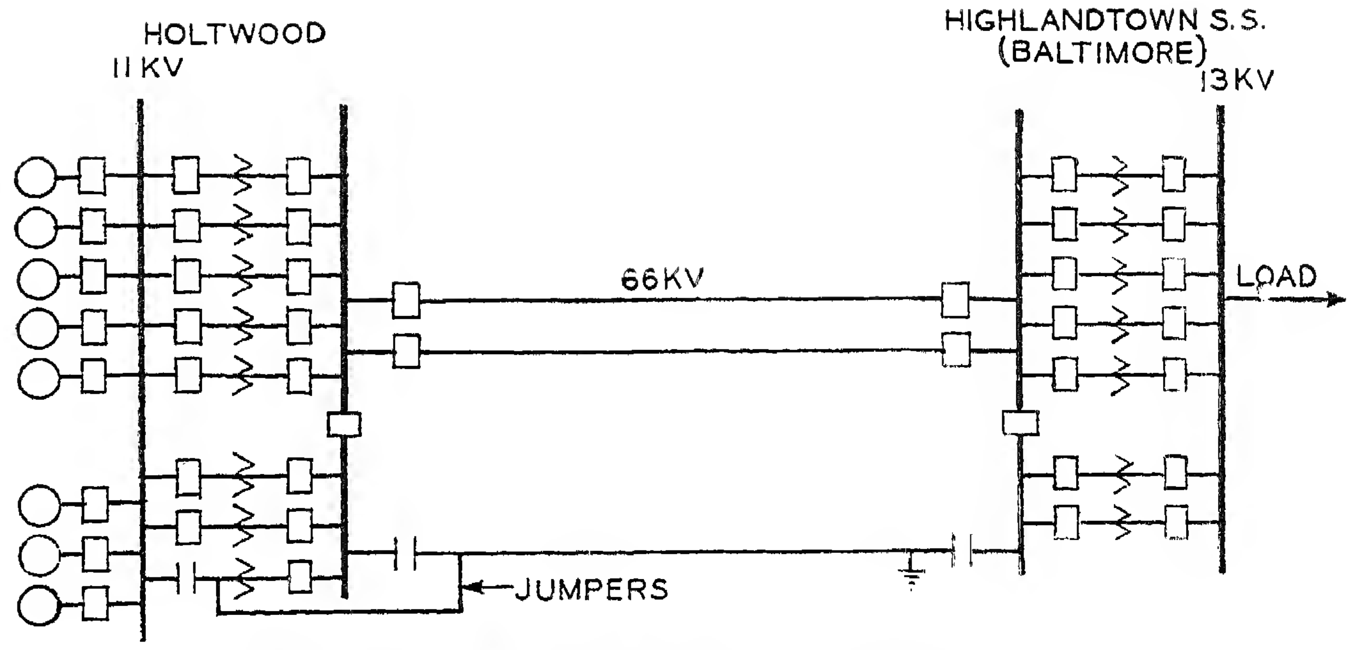

Figure 9. Method of melting sleet, 1916

partrolman is responsible for an average of approximately 30 circuit miles of line and has the necessary material and tools for minor repairs. A heavy line truck is kept in readiness at the line headquarters, loaded with the necessary material and tools to repair any line. Tractors with snow plows are normally kept at two strategic points during the winter.

Circuits are patrolled immediately following every temporary outage and every effort is made to determine the cause, with a high degree of success. In addition, patrols and close inspections are made regularly. All damages found are repaired promptly. Surge crest ammeter links have been useful in locating flashovers from lightning and lightning burns on overhead shielding or ground wires. A fault locator[2] was developed in 1935 and has been valuable in the prompt location of circuit failures.

Of particular importance in the prevention of trouble during sleet conditions is the location and repair of broken strands of the conductors and ground wires. These broken strands may result from lightning and other flashovers and from rifle bullets, and usually can be seen by an experienced patrolman. If not repaired, the weakened cable may fail during ice and low temperature conditions when the tension increases. Fatigue failures of strands have not been experienced since the practice of using comparatively low stringing tensions is followed.

Over the years weak points in design have been corrected, tending to reduce trouble during sleet storms. As an illustration, one such point is cited. After a case of trouble, the original twisted sleeve joints used on the 300,000-circular-mil conductors were reinforced in 1927 by installing a compression joint at each end of the sleeve. These joints were installed without cutting the conductor in a manner similar to the conventional strand repair sleeve in use today. Since that time only compression joints have been used on aluminum, and no further trouble has been experienced.

Trees adjacent to the line are removed or topped before they reach sufficient height to endanger the line during sleet or wind storms. The right-of-way is kept cleared and in first-class condition, which facilitates travel for sleet observation, as discussed now, and greatly facilitates repairs in the event of circuit failure. The same applies to trails and access roads.

Present Method of Combating Sleet

The foregoing historical development culminated in our present method of combating sleet accumulations. This consists of

1. forecasting the appearance of sleet formations;

2. detecting accumulations on conductors; and

3. applying heating current to the conductors to remove the sleet.

Sleet Forecasting

First, it is necessary to forecast sleet in so far as this is possible. This is highly important because time is required to set up system operating conditions which will permit heat runs, such as paralleling additional capacity to release generators for the supply of heating current, returning equipment to service which otherwise would hamper the making of heat runs, and the alerting of personnel to handle the extra work and to man observation posts. The alert is in the form of a 4-hour sleet warning, which is issued by operating personnel and indicates that sleet formation can appear within the next 4 hours. This warning is based on United States Weather Bureau forecasts received by telephone and teletype, airway weather conditions for the area received by shortwave radio, and weather observations on neighboring utility systems as well as on our own. Lack of such a warning system in the past has resulted in a circuit failure being the first indication of sleet formation.

Sleet Detection and Observation

The second step, which is also very important, is the actual detection by visual observation of sleet formation and accumulation on the line conductors. The station personnel at the line terminals and the patrolmen at observation posts along the line provide one observer per 7 miles of line right-of-way. Patrolmen are provided with mobile radio-equipped half-ton trucks and visit known trouble spots at points remote from their posts. Observations and reports are made covering temperature, wind conditions, precipitation, and sleet formation on trees, fences,

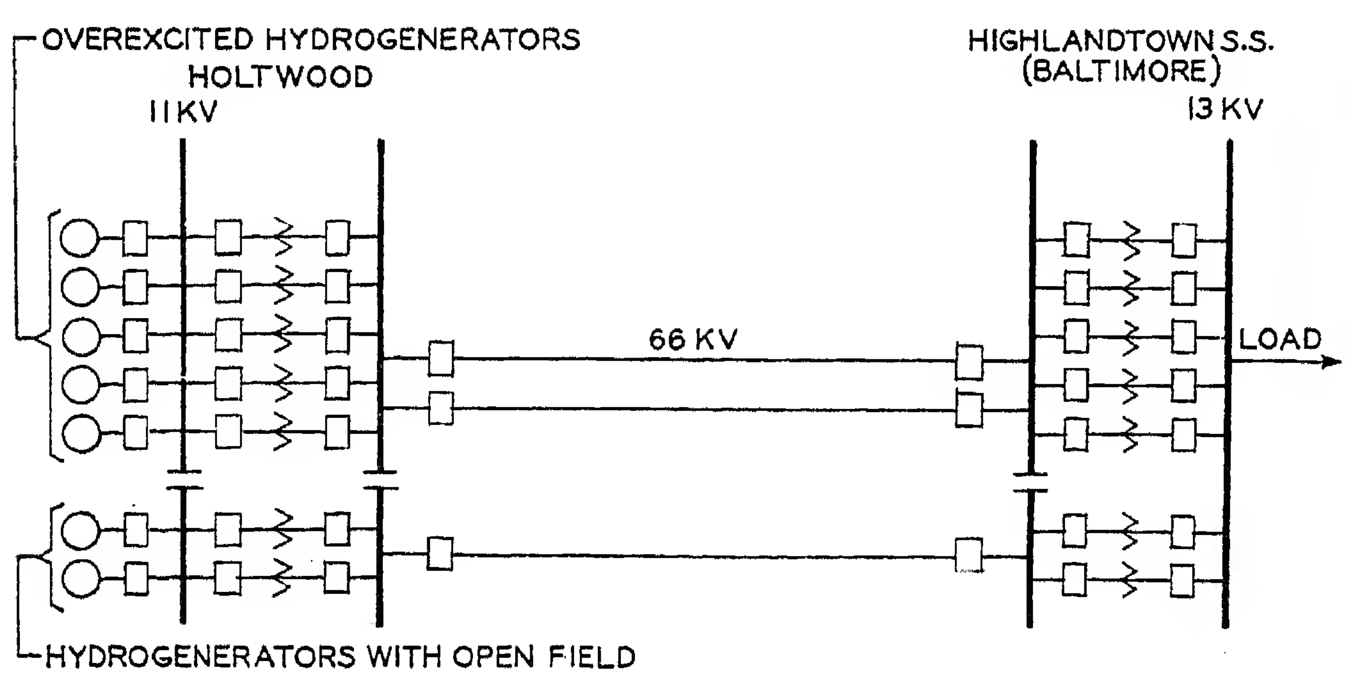

Figure 8. Method of melting sleet during heavy load periods, 1915

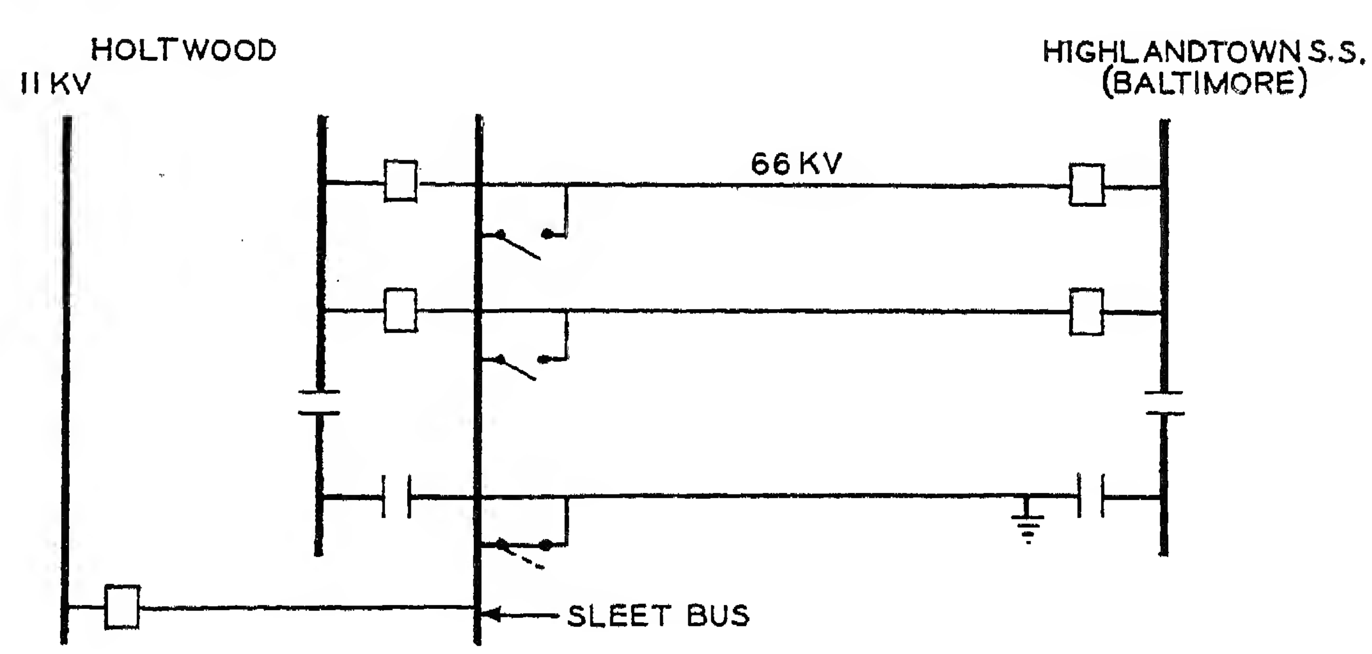

Figure 10. Method of melting sleet, 1916–1932

towers, conductors, and test cables. The test cable is weighed to determine the sleet accumulation on it. Patrolmen climb towers to permit close observation of conductors, and also to obtain temperature at conductor height, which may be significantly different from the temperature near the ground.

Earlier mention has been made of circuit failures caused by sleet formations in a relatively narrow band between observation points, usually resulting from differences in elevation. Residents along the lines have been recruited as weather observers.[3,4] They are equipped with accurate thermometers and instructed as to the collection and reporting of the desired information. They may call the station operator or local patrolman and report their observations, or be called for reports at any time. Usually, they are called by the local patrolman. Incidentally, these observers, as well as other residents along the lines, also report any unusual conditions such as flashovers and line failures during sleet storms, or at any other time.

Mobile radio has been of great assistance in that it enables the patrolman to make prompt reports directly from the observation points. Many more observations can be made since no time is lost traveling to a telephone or waiting for the completion of land line calls which are often abnormally slow and unreliable from rural areas during sleet storms. Also, the operators are practically in continuous communication with the patrolman and can request additional reports at any time, including information as to the effectiveness of the sleet melting. The mobile radio is also valuable in the event travel difficulties are encountered.

An effort has been made to detect sleet formation by observing the value of received carrier signal on the line. However, we have been unable to correlate the carrier signal with visual observations of sleet formation since fog and mist will cause a change in the attenuation of the carrier signal when no sleet formation is present.

Application of Heating Current

The patrolman's observations, and observations collected by him from residents along the line, are reported by radio or telephone to the station operator having jurisdiction of the line. The operator analyzes these data together with other weather data available to him and, based largely on experience, decides when heating current should be applied to a circuit. Influencing the operator in this decision are thickness of sleet on conductors, rate of precipitation, velocity and direction of wind, temperature, temperature trend, and so forth. It is our practice to start heating circuits on which relatively thin sleet formations are present when the other factors are adverse.

Experience has shown that it is not advisable to delay heating circuits during what appears to be a protracted sleet storm. One hour is required for the switching and heating of one circuit. It has been found that with the heating current available and for our area, one sleet bus is needed for each four circuits, that is, each circuit is heated once every 4 hours. Half of the circuits can be heated once each 2 hours by heating two circuits at the same time.

With a fast rate of sleet accumulation, it is important that we start heating circuits promptly and follow the heating sequence as rapidly as possible. It is necessary to keep abreast of the situation at all times. We cannot fall behind in our efforts without hazard to service.

Heating Current Values

Hydrogenerators and frequency changers are used to supply the heating current. Table I shows the normal values of heating current and pertinent conductor data. It should be noted that copper requires approximately twice the watts per square inch as aluminum for a given conductor temperature due to the difference in radiation. The amount of current and the length of time heating current is applied may be varied, within limits, to suit the vagaries of the sleet storm.

Performance of Transmission System Under Sleet Conditions

Figure 11 shows the performance of the transmission system under sleet conditions. By "permanent outage" is meant that the circuit could not be returned to service until repairs were made. Most of the storms which caused the outages shown have been mentioned previously. The performance is considered satisfactory, with the full realization that the complete elimination of outages from sleet cannot be justified economically, particularly on some of the older circuits.

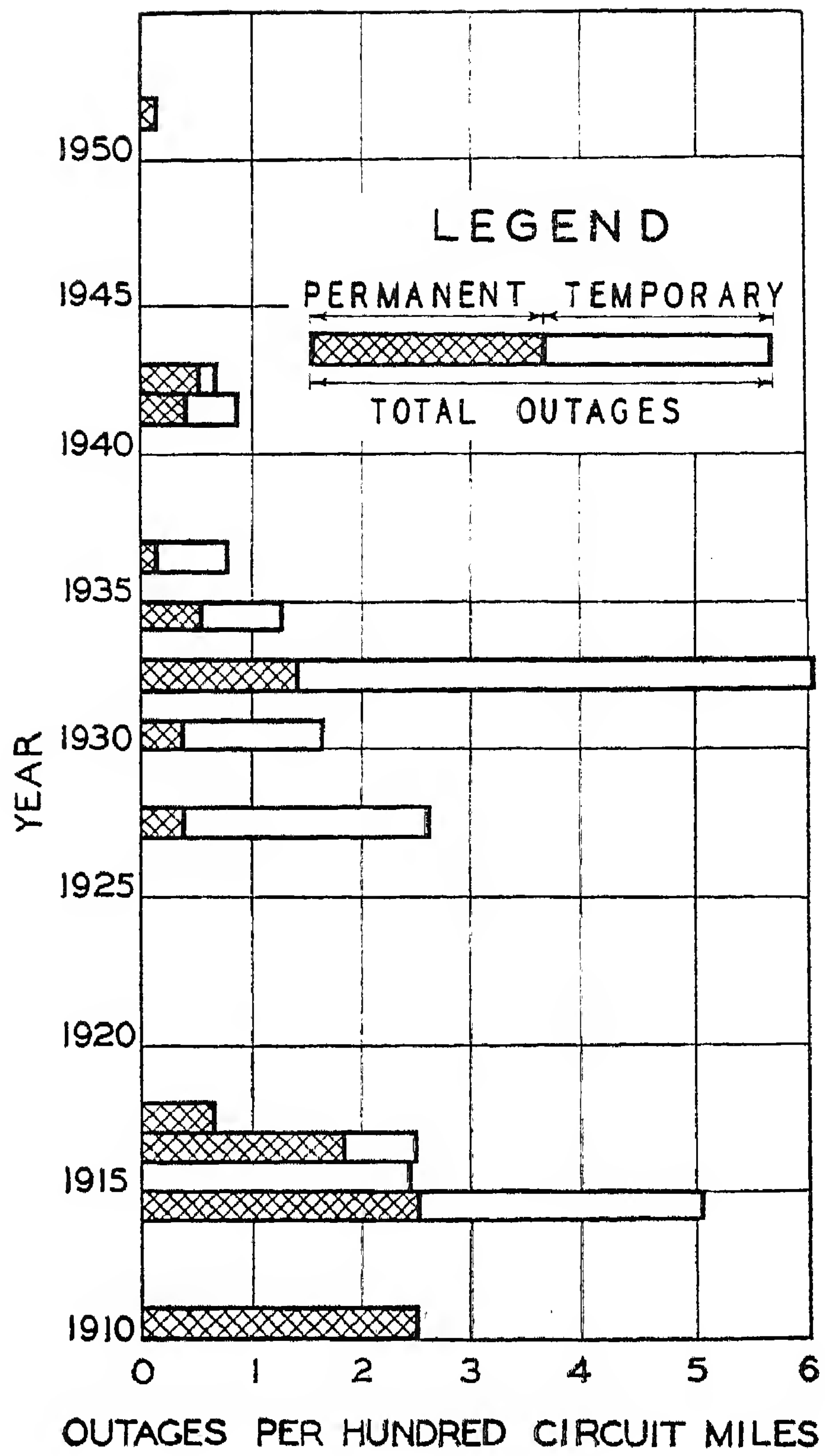

Figure 11. Performance of transmission system under sleet conditions, 1910–1952

We have had no failures of steel towers. It is also interesting to note that we have never had a failure of an overhead ground wire, nor has a ground wire been involved in a sleet outage. This is attributed to the use of relatively low stringing tensions and good preventive maintenance in the location and repair of damaged strands. We have had no outages caused by trees contacting circuits during sleet conditions, which is attributed to good preventive maintenance.

Table I. Circuit Heat Run Data for Conductor 5 Degrees Centigrade

Conductor Size and Material	Number Strands	Amperes	Watts per Square Inch Surface	Minutes Heat Is Applied
2/0 copper	7	450	0.728	40
300,000-circular-mil aluminum	19	425	0.295	40
397,500-circular-mil steel-reinforced aluminum cable	30/7	600	0.326	60

Conclusions

A summary of our experience results in the following conclusions:

1. Adequate line design including mechanical strength, conductor spacing, and configuration is required.

2. In our territory, horizontal configuration steel tower lines can be built to be highly sleet resistant and to give nearly

perfect performance, without sleet melting. The savings resulting from absence of necessity for sleet-melting facilities or for removal from commercial operation to melt sleet, should not be overlooked in considering this type of design.

3. Methods of combating sleet as described herein will provide satisfactory performance on lines which do not have adequate conductor spacing and configuration.

4. High-speed relaying is essential to limit the damage of conductor strands during flashover due to sleet or other causes, so that lines will not burn down.

5. Adequate preventive maintenance is necessary, especially in respect to broken strands, danger trees, right-of-way, and roads and trails.

6. Adequate sleet observations and weather reports are necessary.

7. Mobile radio communication is essential.

8. The forecasting and detection of sleet accumulations and evaluations of sleet reports involve the human element. In so far as this element fails, our procedures are less effective.

9. Sleet busses with gang-operated disconnectors are necessary to expedite the heat runs.

10. Higher sleet melting currents are required when storms are accompanied by high winds and very low temperatures. These have occurred in our area at about 10-year intervals.

11. The economic limit has been reached in combating sleet accumulations on our system. This is borne out by Figure 11.

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No Discussion

New Apparatus Bushing with Improved Characteristics

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THE objective of the skilled bushing designer is to obtain improved characteristics while conserving copper and other critical materials. Present and prospective future shortages in the supply of these materials challenge the designer to produce a product of improved quality.

The objective of combining improved performance with saving in materials has been accomplished in the redesign of a line of liquid-filled bushings in ratings of 15 kv through 69 kv. In this redesign, the mechanical and electrical reliability of the bushings in service has been improved and field servicing and repairs have been facilitated. Simultaneously, the design has been simplified to reduce the size of some parts and eliminate others.

The basic design of the superseded bushing was introduced in 1932 and marked the first general use of rubber-like gaskets in bushings to give a dependable pressure-tight seal. Since then nearly all bushing designs have been changed from the former composition cork gaskets to the use of rubber-like gasket materials. The resin-impregnated paper insulator around the conductor continues to offer the best combination of high dielectric strength and high mechanical strength. During the period since 1932, the basic design has been improved on numerous occasions and over 150,000 of these bushings have been placed in service.

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Description and Advantages of the New Construction

On the left of the center line of Figure 1 is shown the old design while on the right of the center line the new design is illustrated. This figure shows the reduction in over-all length obtained by the new design. This reduction results in a shorter core tube with consequent saving in material.

The decrease in length results mainly from the elimination of the cemented-on clamping rings which are used in the old design to flange-clamp the porcelain to the support and top washer. With the old construction, it is necessary to exercise care in assembling and in mounting the bushing since excessive or uneven bolting torque may break the porcelain shell. The new design is center-clamped. This means that the porcelain is placed in compression, under which stress porcelain has the greatest strength. The center clamping force is exerted by threading the cap down against a Belleville spring which is a dished circular washer of high-strength spring temper alloy. This Belleville spring serves as the flexible member between the center conductor and the bushing shell and also as the top end seal of the bushing chamber. Since the mounting flange is not connected directly to the porcelain on the new design, bolting torque applied in mounting the bushing to the apparatus cannot damage the porcelain shell.

Both the old and the new design are oil-filled to insure high internal strength. Part of the expansion space for the oil in the old design was provided by the cap and part in the upper few inches of the porcelain shell. In the new design the larger internal porcelain diameter makes it possible to provide all of the expansion space in the upper portion of the shell. This method of providing for oil expansion allows a reduction in the physical size of the metal parts.

The elimination of the cemented-on clamping rings in the new design not only saves the material in the rings but also reduces the manufacturing cycle. With cemented-on rings a considerable time must be allowed so that the cement will cure properly before it is placed under mechanical stress.

Aluminum is used extensively in the new design to reduce the weight. The core tube of all 400-ampere through-cable designs is aluminum. In this design the tube does not carry current. The support of the new design is of high-strength heat-treated aluminum. The ground sleeve for the new bushing is aluminum tubing manufactured to close tolerance.

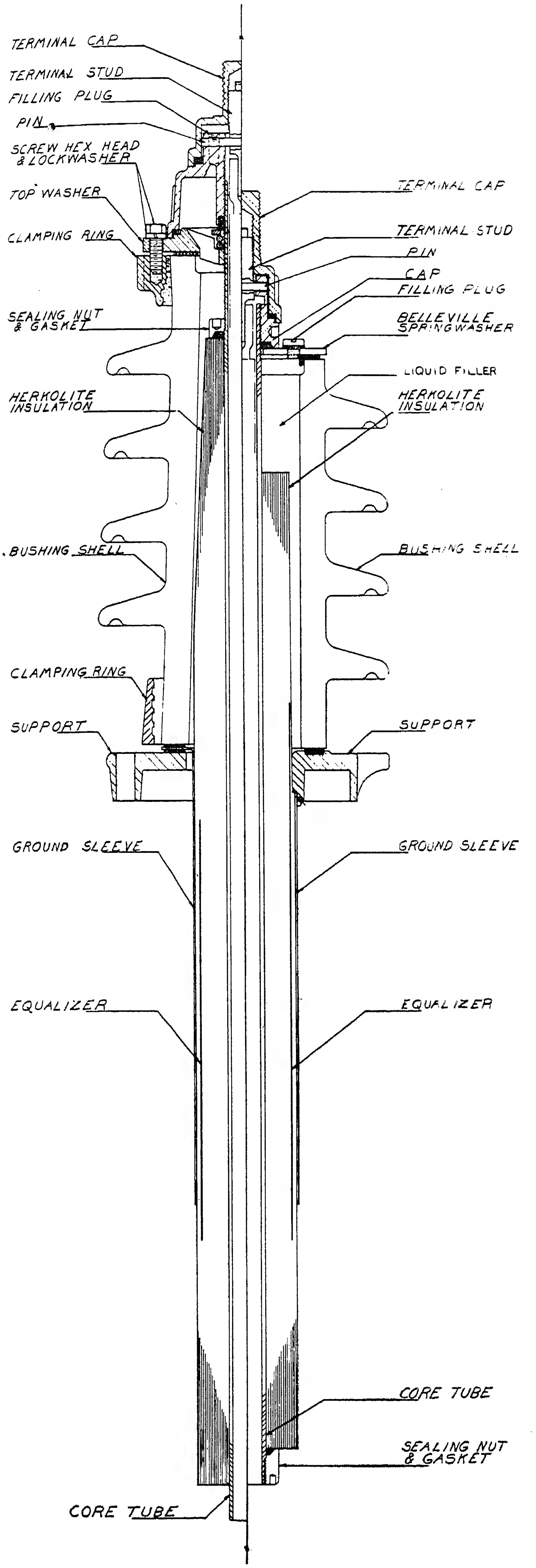


Figure 1. Cross-sectional view showing old and new transformer bushing

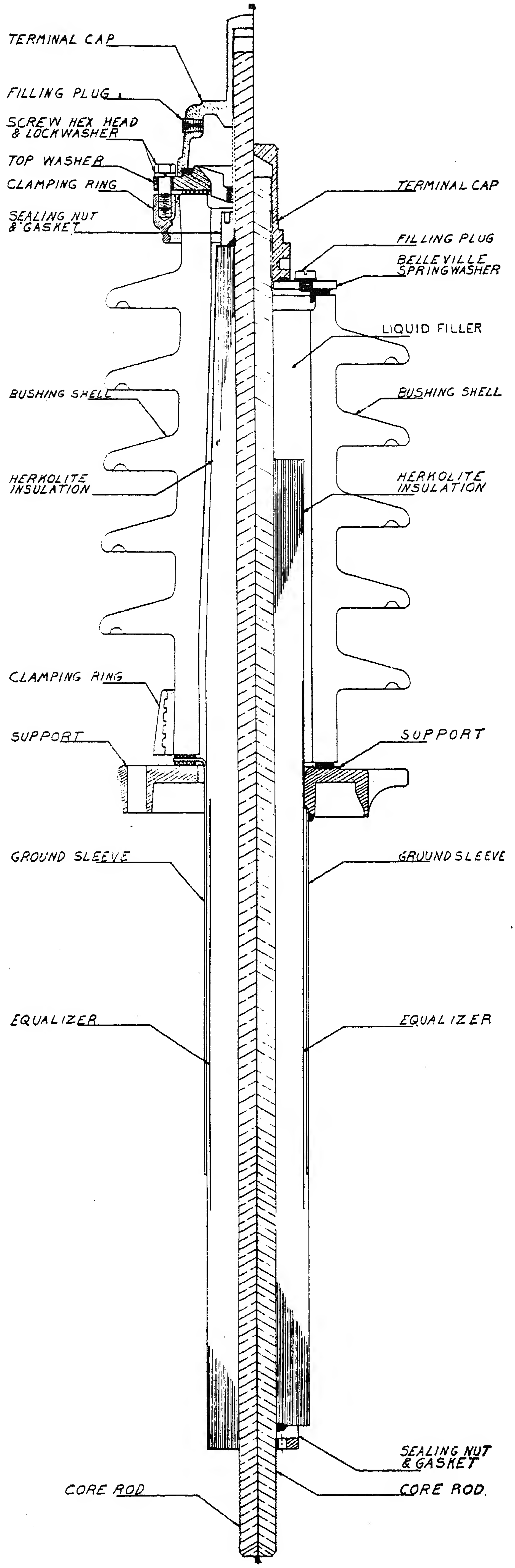
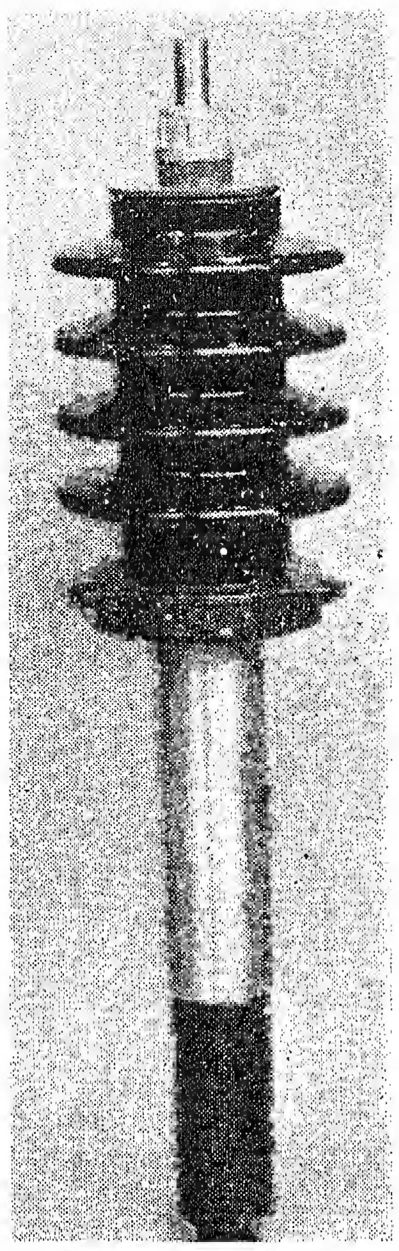


Figure 2. Cross-sectional view showing old and new oil circuit-breaker bushing design



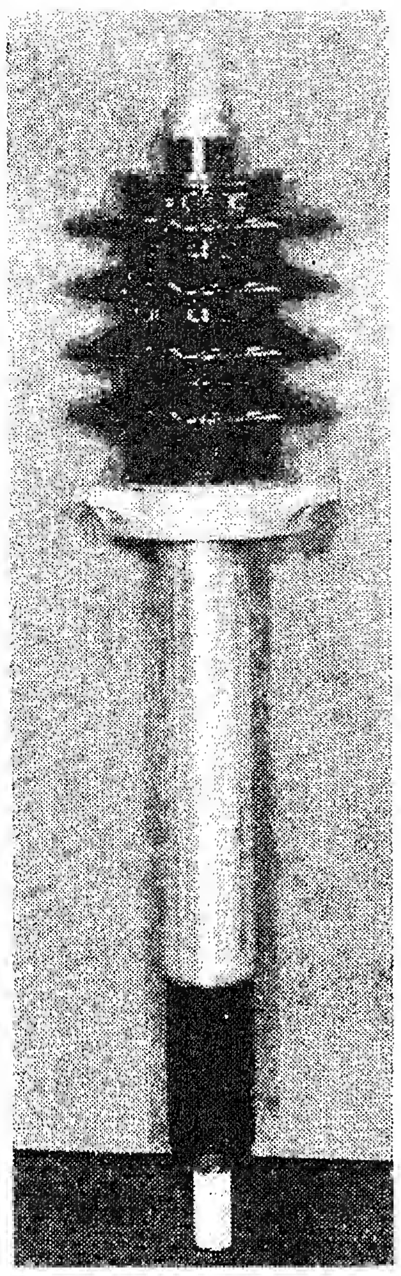


Figure 3 (left). Center-clamped bushing for transformers rated 34.5 kv

Figure 4 (right). Center-clamped bushing for oil circuit breakers rated 25 kv

Close tolerances in ground sleeve diameters along with accurate machining of the Herkolite insulation, and a press and shrink fit between these parts, results in a strong and permanent bond between the ground sleeve and the Herkolite insulation.

The support of the new design also is assembled to the Herkolite insulation and to the ground sleeve with a press and shrink fit. This construction results in a strong mechanical joint. The oil seal between the support and the Herkolite core is obtained by the use of a rubber gasket. This gasket is completely confined and provides a permanent seal.

Should a porcelain shell be broken accidentally, field repairs can be made on the new design with considerably less difficulty than with the old bushing. The old bushing had to be removed completely from the apparatus for repairs but with the new design the mounting flange can remain bolted in place during removal of the old porcelain and replacement with a new one.

The reduction in weight of the new design is approximately 20 per cent, most of which is critical material. This facilitates packing for shipment, handling, and installation.

Both the old and new design employ an embedded conducting equalizer to eliminate the possibility of corona at the lower end of the ground sleeve at applied voltages up to and including test levels.

Development Tests of the New Design

Thermal Tests

On a center clamped bushing, such as the new design, proper follow-up on gasket seals is essential if the design is to remain pressure-tight through the normal temperature cycles experienced in service. The Belleville washer provides adequate spring follow-up in the new design as verified by the following heating and cooling tests.

The center conductor of the bushing was heated to 100 degrees centigrade. The bushing then was allowed to cool to room temperature. These cycles were repeated over 100 times without the occurrence of oil leakage.

As an additional test the bushing was placed in a cold chamber at −40 degrees centigrade for 24 hours. It then was heated internally to a temperature of 100 degrees centigrade for 8 hours. The bushing remained pressure-tight during and after the completion of this test.

Mechanical Tests

Momentary or maintained imposition of an excessive transverse load on the top end of the porcelain is of serious consequence because in many bushing designs it results in a broken porcelain. The new design is stronger against the effects of transverse loading than the old design and the results of an excessive force are less serious.

The porcelain on the new design is in compression. The application of a transverse force on the bushing increases the compressive force on one side of the porcelain and decreases it on the other. If the transverse force is severe, oil leakage will result but porcelain breakage rarely occurs.

In the new design the center clamping force places an axial load on the support and ground sleeve. Tests were necessary, therefore, to verify that this construction would hold the support in position against the center clamping force. These tests indicate that the force necessary to move the support on the Herkolite is several times the center clamping force.

During circuit-breaker interrupting tests, which are necessary to prove the adequacy of any new design, axial and transverse loads are imposed on the bottom end of the bushing. Each rating of the new design has taken these tests without incident. The old and new oil circuit bushings are shown in cross-section in Figure 2, to the left and right of the center line respectively.

A special coating is added to the bottom insulation of the new circuit-breaker bushing to inhibit the deposition of carbon particles on this surface.[1] The old design required a bottom porcelain and other bracing when applied on oil circuit breakers. The new design, due to its greater mechanical strength, can operate in a circuit breaker without a bottom porcelain. This omission of the bottom porcelain facilitates inspection and cleaning of the bottom end of the bushing.

For added strength to withstand the axial forces consequent to oil circuit-breaker operation the new design has Herkolite shoulders at the bottom end of the ground sleeve and at the support. These two Herkolite shoulders combine to give the core assembly an axial strength many times that of the center clamping force of the bushing.

The center clamping force subjects the bond between the Herkolite core and the center conductor to shear. The shear strength of this bond is several times greater than the force imposed upon it. The nut and gasket which seal the lower end of the Herkolite to the center conductor further prevent movement of the center conductor in the Herkolite core.

Electrical Tests

The new design is electrically equivalent to the old design in all ratings and complies with all requirements of AIEE and National Electrical Manufacturers Association Standards. Each rating of the new design was subjected to ten 60-cycle 1-minute rated withstand tests interspersed by ten 60-cycle flashovers and followed by 30 minutes at the 1-minute rated withstand voltage. No rise in power factor or other indication of deterioration was noted upon completion of these tests.

Two 34.5-kv bushings have been on life test for over 1 year at 175 per cent of their rated voltage. Frequent electrical tests on these bushings indicate that there have been no deleterious effects.

Summary

A new line of center-clamped bushings for ratings of 15 kv through 69 kv has been developed. The new design for transformers is shown in Figure 3 and the new design for oil circuit breakers in Figure 4.

The new design has been tested thoroughly. It has been proven equal electrically and superior mechanically to the superseded design.

Principal features of the new design are:

1. Simplified design.

2. Shorter manufacturing cycle.

3. Broken porcelain can be replaced easily in the field without removing bushing from apparatus.

4. Lighter weight facilitates handling in the field.

5. Rugged construction, attained by placing porcelain in compression.

6. Bottom porcelain not required for circuit-breaker applications.

7. Use of less critical material.

The new design incorporates the time-tested good features of the old design, and should establish an even more enviable record of service to users.

Reference

1. OPERATION OF BUSHINGS IN CARBONIZED OIL, L. Wetherill, W. R. Wilson. *AIEE Transactions*, volume 70, part II, 1951, pages 1398-1407.

No Discussion

Sleet-Melting Practices—Niagara Mohawk System

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EXPERIENCE with sleet conditions on the Niagara Mohawk System has indicated that adequate planning is necessary if satisfactory results are to be secured from any sleet-melting program. This planning extends from system design to operating procedures to training. All must be co-ordinated to weather a severe storm successfully.

Included in this planning is close contact with the United States Weather Bureau. Bureau personnel should be alerted as to power company's problems so that advance warning of critical conditions may be made available. Information on possible troublesome areas, duration of storm period, possible ice accumulation, temperatures, and wind velocities will be of great value in arranging the melting program.

The types of storms that cause difficulty can in general be placed in two classifications. The first consists of a light freezing rain which deposits a thin coating of ice on the conductor. This type of storm is usually followed by a windy period which, together with the change in the airfoil shape of the conductor, causes the phenomena known as "galloping or jumping conductors." Galloping of the conductors causes short circuits with subsequent trip outs and in extreme cases may cause structural failure of the transmission towers. The second type of storm is a continuing freezing rain extended over several hours, which builds up a heavy ice load on the conductor. If allowed to accumulate the ice load in itself may cause tower or conductor failure. A combination of strong winds and heavy ice loading is particularly dangerous.

Both of these storm conditions usually start with about the same weather conditions, that is, moderate temperature, very little wind, and cold metal surfaces. If the storm center passes over quickly, heavy winds follow and galloping conductors result. Should the storm center remain stationary, temperatures may drop and heavy ice formations may accumulate. In any case, however, there is usually a period of moderate temperature and little wind when sleet preventing or melting procedures are most effective. Prompt action at this time may save

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Table I. Conductor Safe Ampere Limits for Copper

Maximum Conductor Temperature of 203 Degrees Fahrenheit (95 Degrees Centigrade)

Air Temperature, Degrees Fahrenheit	Conductor Size, Circular Mils	Wind Velocity in Miles per Hour						
		0	2	10	20	30	40	50
104	4/0	411	526	788	936	1,036	1,115	1,180
	250,000	463	586	876	1,040	1,153	1,237	1,307
	300,000	527	653	979	1,165	1,290	1,385	1,465
	350,000	585	724	1,080	1,280	1,420	1,530	1,610
	400,000	641	792	1,170	1,395	1,540	1,660	1,750
	500,000	751	890	1,340	1,585	1,750	1,890	2,000
90	4/0	440	564	844	1,000	1,110	1,193	1,265
	250,000	494	628	940	1,115	1,235	1,325	1,400
	300,000	560	700	1,048	1,247	1,380	1,480	1,565
	350,000	625	774	1,155	1,370	1,520	1,630	1,730
	400,000	685	850	1,250	1,495	1,650	1,780	1,870
	500,000	800	950	1,430	1,695	1,875	2,020	2,140
80	4/0	459	590	884	1,050	1,162	1,247	1,322
	250,000	516	658	982	1,167	1,293	1,385	1,470
	300,000	585	733	1,097	1,305	1,445	1,550	1,640
	350,000	653	810	1,210	1,430	1,590	1,710	1,810
	400,000	714	890	1,310	1,565	1,730	1,860	1,960
	500,000	837	1,000	1,500	1,780	1,965	2,120	2,240
70	4/0	481	612	915	1,088	1,205	1,295	1,370
	250,000	540	681	1,020	1,210	1,340	1,440	1,520
	300,000	614	758	1,137	1,352	1,497	1,610	1,700
	350,000	683	840	1,255	1,485	1,645	1,770	1,870
	400,000	750	920	1,360	1,620	1,790	1,930	2,030
	500,000	875	1,035	1,550	1,840	2,040	2,200	2,320
60	4/0	500	638	955	1,135	1,257	1,350	1,430
	250,000	562	711	1,062	1,262	1,400	1,500	1,590
	300,000	639	792	1,186	1,410	1,565	1,680	1,770
	350,000	710	876	1,308	1,550	1,720	1,850	1,950
	400,000	778	960	1,420	1,690	1,870	2,010	2,220
	500,000	910	1,080	1,620	1,920	2,120	2,290	2,420
50	4/0	520	660	985	1,173	1,300	1,395	1,480
	250,000	584	734	1,098	1,305	1,445	1,550	1,640
	300,000	662	818	1,225	1,460	1,615	1,730	1,830
	350,000	737	905	1,350	1,600	1,775	1,910	2,020
	400,000	808	990	1,460	1,750	1,930	2,080	2,190
	500,000	946	1,115	1,675	1,980	2,190	2,370	2,500

Table II. Conductor Safe Ampere Limits for Copper

Maximum Conductor Temperature of 203 Degrees Fahrenheit (95 Degrees Centigrade)

Air Temperature, Degrees Fahrenheit	Conductor Size, Circular Mils	Wind Velocity in Miles per Hour						
		0	2	10	20	30	40	50
40	4/0	540	680	1,020	1,210	1,340	1,440	1,525
	250,000	605	758	1,135	1,345	1,490	1,600	1,690
	300,000	687	844	1,265	1,505	1,665	1,790	1,890
	350,000	765	935	1,395	1,650	1,830	1,970	2,090
	400,000	840	1,020	1,510	1,800	2,000	2,150	2,260
	500,000	982	1,150	1,730	2,050	2,260	2,440	2,585
30	4/0	560	703	1,050	1,250	1,380	1,485	1,572
	250,000	628	782	1,170	1,390	1,540	1,650	1,750
	300,000	714	870	1,305	1,553	1,720	1,850	1,950
	350,000	794	965	1,440	1,705	1,890	2,030	2,150
	400,000	873	1,050	1,560	1,860	2,060	2,220	2,330
	500,000	1,020	1,190	1,780	2,110	2,335	2,520	2,665
20	4/0	576	718	1,075	1,280	1,415	1,520	1,610
	250,000	647	800	1,195	1,420	1,570	1,685	1,785
	300,000	732	890	1,335	1,588	1,760	1,890	1,990
	350,000	815	986	1,470	1,740	1,930	2,080	2,200
	400,000	893	1,080	1,590	1,900	2,100	2,270	2,390
	500,000	1,046	1,215	1,820	2,160	2,390	2,580	2,730
10	4/0	593	740	1,105	1,315	1,455	1,560	1,660
	250,000	666	823	1,230	1,460	1,620	1,740	1,840
	300,000	754	918	1,375	1,635	1,810	1,940	2,050
	350,000	840	1,015	1,515	1,795	1,990	2,140	2,260
	400,000	922	1,110	1,640	1,960	2,170	2,330	2,460
	500,000	1,078	1,250	1,880	2,220	2,460	2,650	2,810
0	4/0	610	755	1,130	1,345	1,490	1,600	1,690
	250,000	684	841	1,260	1,490	1,650	1,775	1,880
	300,000	776	937	1,403	1,670	1,850	1,980	2,100
	350,000	865	1,037	1,550	1,830	2,030	2,190	2,310
	400,000	947	1,140	1,670	2,000	2,210	2,380	2,510
	500,000	1,108	1,280	1,920	2,270	2,510	2,710	2,870

Table III. Sleet-Preventing Amperes for Copper

Conductor Temperature Raised to 34 Degrees Fahrenheit

Air Temperature, Degrees Fahrenheit	Conductor Size, Circular Mils	Wind Velocity in Miles per Hour					
		2	10	20	30	40	50
30	4/0	106	159	189	209	225	238
	250,000	118	177	210	233	250	265
	300,000	132	198	235	260	280	296
	350,000	146	218	258	286	308	326
	400,000	160	236	282	312	336	354
	500,000	180	270	320	354	382	404
25	4/0	160	239	284	314	338	358
	250,000	178	266	315	350	375	397
	300,000	198	297	353	391	419	443
	350,000	219	327	387	429	462	489
	400,000	240	354	423	468	504	531
	500,000	270	405	480	531	573	606
20	4/0	199	298	353	391	421	445
	250,000	222	331	393	436	468	495
	300,000	247	370	440	487	522	553
	350,000	273	408	483	536	576	610
	400,000	300	442	528	584	628	662
	500,000	337	505	598	662	715	756
15	4/0	232	347	412	457	490	520
	250,000	259	386	458	508	545	577
	300,000	288	430	513	568	609	643
	350,000	318	475	562	624	671	710
	400,000	349	514	614	680	732	771
	500,000	392	588	698	770	832	880

hours of work later or possibly a system calamity. Older melting procedures were cumbersome and time consuming so that it was often impossible to take advantage of the favorable period for sleet prevention or melting.

Some years ago Niagara Mohawk recognized the shortcomings of the then existing sleet-melting program and initiated studies to improve the situation. Tables were to be developed showing required sleet preventing or melting currents for conductors under varying weather conditions. A-c network analyzer studies followed, to determine system setups most adaptable for sleet-melting purposes. In some cases these studies indicated that changes in normal system connections would be advantageous.

Co-ordination of these studies resulted in the following:

1. The Niagara Mohawk System recognized three principal methods of sleet prevention or sleet melting.

   Load Current Method. The heating effect of load currents is used to prevent or remove ice on the conductor. Where necessary, normal operating conditions are modified in order to force more load current through a particular circuit or circuits. Use of this procedure is emphasized since switching is performed at manually operated stations, no markups are necessary, and a minimum of time is required to make the procedure operative.

   Short-on-a-Generator Method. One or more generators are isolated from the rest of the system and connected to a circuit, which has also been isolated. The three phases of this isolated circuit are short-circuited at the far end. Generator field is gradually increased (from a subnormal value) to a value sufficient to produce the desired melting current.

   Short-on-a-Bus Method. One or more transmission circuits are isolated from the rest of the system. The total length of these transmission circuits is predetermined so that, with normal operating voltage applied at one end and a 3-phase short circuit established at the far end, the desired sleet-melting current will flow. Then, having established the 3-phase fault, the near end is connected to its regular bus (at normal voltage) through a circuit breaker.

2. System connections were changed as indicated by the a-c network analyzer studies. In one instance a complete auxiliary bus was added at a station to facilitate sleet melting. The final decision as to design of two new 115-kv generating station busses was influenced by their adaptability for sleet-melting purposes.

3. Step-by-step instructions and detailed drawings were developed for the various sleet-melting procedures.

Sleet-Melting Tables for Overhead Electric Power Conductors

Included in the instructions and material for use by the operating personnel are sleet-melting tables for overhead electric power conductors. Tables I–VI were compiled to facilitate and speed up sleet-melting operations on overhead electric power conductors. Included are tables to determine sleet-melting current, sleet-preventing current, and conductor safe current limit.

The data were obtained through the use of an alignment chart (nomogram), which was developed on the basis of formulae presented by D. C. Stewart.[1]

It is felt that the tables have sufficient range to handle all possible weather conditions. To minimize the work involved, conductor sizes were limited to six of stranded copper and six of steel-reinforced-aluminum cable. However, more may be added if necessary. Interpolation will give results of sufficient accuracy.

For all practical purposes, melting-current amperes to the nearest tens digit would have been accurate enough, especially in view of the fact that the basic data (wind velocity, ice thickness, and so forth) cannot be exact. However, current values tabulated are as read directly from the alignment chart. Obviously in sleet-melting operations these exact values should be of no concern.

The tables are divided into two main sections: copper, Tables I–III, and steel-reinforced-aluminum cable, Tables IV–VI. Tables I and II give conductor safe ampere limits and Table III gives sleet-preventing amperes for all six conductor sizes. These three tables show six conductor sizes (4/0, 250,000, 300,000, 350,000, 400,000, and 500,000 circular mils). Tables IV–VI give the sleet-melting amperes for 1/10 inch, 1/4 inch, and 1/2 inch of ice respectively. The steel-reinforced-aluminum-cable tables contain the same material for 336,400-, 397,500-, 477,000-, 556,500-, 636,000-, and 795,000-circular-mil conductors.

Separate tables are not provided for all-aluminum conductors. Satisfactory results may be obtained by using Tables IV–VI for some steel-reinforced-aluminum-cable conductors:

1. For 428,000 circular-mil aluminum, use 397,500 circular-mil steel-reinforced-aluminum-cable tables.
2. For 556,500 circular-mil aluminum, use 556,500 circular-mil steel-reinforced-aluminum-cable tables.

In determining sleet-melting or sleet-preventing currents, the largest conductor in any one circuit governs. However, in determining the safe ampere limit for a circuit, the smallest conductor in the circuit governs. Therefore, since in the Niagara Mohawk System there are some circuits containing 4/0 steel-reinforced-aluminum-cable conductor in series with larger conductors, 4/0 steel-reinforced-aluminum cable has been included only in the safe ampere limit tables.

In determining the safe ampere limit for any circuit, it must be kept in mind that the tables herein are for the conductor only. The safe ampere limit for terminal equipment should be checked also by referring to local operating bulletins.[2]

Since Tables I–VI were intended for use in sleet prevention as well as sleet melting,

Table IV. Sleet-Melting Amperes for 350,000-Circular Mil Cable, 1/10 Inch Ice

Temperature, Degrees Fahrenheit	Melting Time, Minutes	Wind Velocity in Miles per Hour					
		2	10	20	30	40	50
30	10	500	510	520	530	535	540
	20	360	375	395	400	410	415
	30	310	325	345	355	360	370
	40	265	290	310	315	325	335
	50	245	265	290	300	310	320
25	10	530	575	605	625	645	660
	20	400	460	500	520	545	560
	30	355	420	460	485	510	525
	40	320	395	435	460	485	505
	50	300	375	420	450	475	495
20	10	555	630	680	710	735	760
	20	435	530	585	620	650	675
	30	395	495	555	590	620	655
	40	360	470	535	570	600	630
	50	345	455	520	560	590	620
15	10	585	680	745	785	820	845
	20	475	585	660	705	740	770
	30	435	555	635	680	720	745
	40	410	535	615	660	700	730
	50	395	520	605	655	690	720

Table V. Sleet-Melting Amperes for 350,000 Circular Mils, 1/4 Inch Ice

Temperature, Degrees Fahrenheit	Melting Time, Minutes	Wind Velocity in Miles per Hour					
		2	10	20	30	40	50
30	10	785	795	800	805	810	815
	20	565	580	585	595	600	605
	30	460	480	485	500	505	510
	40	400	420	430	440	450	455
	50	360	385	395	410	415	420
25	10	805	840	860	875	885	895
	20	595	640	665	685	700	715
	30	500	550	580	600	620	635
	40	440	500	535	555	575	590
	50	410	470	505	525	550	565
20	10	830	880	915	940	960	975
	20	625	690	735	765	790	805
	30	535	610	660	690	720	740
	40	480	565	615	655	680	700
	50	450	540	590	630	655	680
15	10	850	920	970	1,000	1,020	1,045
	20	655	740	800	835	865	890
	30	565	665	730	770	800	830
	40	515	625	695	735	765	795
	50	485	600	670	715	745	780

Table VI. Sleet-Melting Amperes for 350,000 Circular Mils, 1/2 Inch Ice

Temperature, Degrees Fahrenheit	Melting Time, Minutes	Wind Velocity in Miles per Hour					
		2	10	20	30	40	50
30	10	1,095	1,100	1,105	1,110	1,110	1,115
	20	780	790	795	800	805	805
	30	640	650	660	665	665	670
	40	560	570	580	580	590	595
	50	505	515	530	535	540	545
25	10	1,110	1,135	1,150	1,160	1,170	1,175
	20	805	835	855	870	880	890
	30	665	705	730	745	760	765
	40	590	635	665	680	695	700
	50	540	585	615	635	650	660
20	10	1,130	1,170	1,195	1,210	1,225	1,235
	20	830	885	920	935	955	965
	30	695	760	800	825	845	855
	40	625	695	740	760	785	795
	50	575	655	695	720	745	760
15	10	1,150	1,205	1,240	1,260	1,275	1,290
	20	855	925	975	1,000	1,020	1,030
	30	725	810	865	890	915	930
	40	660	750	805	835	860	880
	50	610	710	770	800	825	845

operating personnel were given the following information.

1. Sleet prevention. Conductors maintained at a temperature slightly above freezing will not accumulate ice. The current necessary will depend on

   Air temperature
   Wind velocity
   Conductor cross section and conductivity

Table III indicates these currents.

2. Sleet melting. Once ice has formed on the conductors it is necessary to go to sleet-melting currents, which, obviously, will be greater than sleet-preventing currents. The required current depends on

   Thickness of ice formation
   Air temperatures
   Wind velocity
   Amount of time available to remove the ice
   Conductor cross section and conductivity

By means of Tables IV–VI the proper current can be determined quickly.

A comparison of any two corresponding sleet-preventing and sleet-melting currents, that is, before and after ice has formed, will emphasize the importance of acting before conditions become unfavorable.

For example, Table III indicates that with a 30-degree temperature and 2-mile wind only 146 amperes flowing through a 350,000-circular-mil copper conductor will prevent sleet accumulation on the conductor. Tables IV and VI point out that once ice has formed and the usual unfavorable weather conditions develop, much heavier melting currents are required depending on the ice thickness, air temperature, wind velocity, and time required to move the ice. Typical examples are:

Air Temperature, Degrees	Ice Thickness, Inches	Wind Velocity, Miles per Hour	Melting Time, Minutes	Current Required, Amperes
Sleet Prevention				
30	0	2		146
Sleet Melting				
30	0.1	2	50	245
25	0.1	10	30	420
20	0.5	20	30	800

As much of this program was new, it seemed desirable to acquaint operating personnel with procedures. As a training program several of the sleet-melting connections were actually set up on the system and melting currents were applied for short periods.

Conclusions

Since the adoption of this program several light sleet storms have been weathered without disrupting the system. Procedures developed have worked successfully under conditions that normally would have caused trouble.

Further experience will be necessary with storms where heavy ice formations accumulate before this program can be fully evaluated.

References

1. Removal of Ice From Transmission Line Conductors, **D. C. Stewart.** *Bulletin*, Edison Electric Institute (New York, N. Y.), May 12, 1936.

2. Western Division Operating Practice. *Bulletin E 7-6*, Niagara Mohawk System, Buffalo, N. Y., August 15, 1951.

No Discussion

Artificial Cooling of Power Cable

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The artificial cooling of electric machinery is a practice which is familiar to almost all electrical engineers. In the case of rotating machinery, the rotating element usually provides a considerable circulation of air; such rotating elements frequently are furnished with fan blades, which further promote the circulation of air in the machine. Large machines often are cooled by means of external fans and forced air cooling of transformers is not uncommon. Transformers are cooled also by circulating water through copper pipes immersed in the oil at the top of the tank, and frequently entire substations or portions thereof are cooled by forced ventilation.

Artificial cooling, however, has been rather unusual in the past in cable work. But with the growth of load, and the increased congestion which occurs in substations, power stations, and their immediate neighborhood, the heat problem has become increasingly severe so that there is a tendency nowadays to consider what can be done to reduce conductor sizes by means of artificial cooling.

Removal of Heat from Cables

The heat produced by losses in cables theoretically can be removed by conduction, convection, radiation, or evaporation. The last-mentioned of these techniques is rarely, if ever, used, since the liquids available for evaporation are usually either too costly or tend to damage the cable finish by causing rotting, corrosion, or some similar phenomenon.

In fact, it is somewhat unusual for more than two of these possible four methods to be available in practice in any given case. A cable suspended in free air dissipates its heat by convection and radiation; the supports which carry such a cable rarely, if ever, furnish enough conduction to assist materially in heat dissipation. A cable in a duct likewise transmits all its heat to the duct bank by convection and radiation; seldom does conduction at the point of contact between the cable and the duct play a material part in cooling the cable.

On the other hand, transmission of heat from a cable immersed in oil to the pipe surrounding it is a matter of conduction and convection, as the fluid tends to suppress radiation to a great extent; whereas a buried cable has to get rid of all its heat by conduction through the earth without benefit of normal convection or radiation.

Improvement of Cooling Conditions

The cooling of a long cable line by artificial means is apt to be a very difficult and costly matter; however, it is often possible to improve a difficult hot spot situation by artificial cooling, without undue expense, especially in or near a generating station or substation.

In the case of an underground cable it is possible to mitigate hot spot conditions

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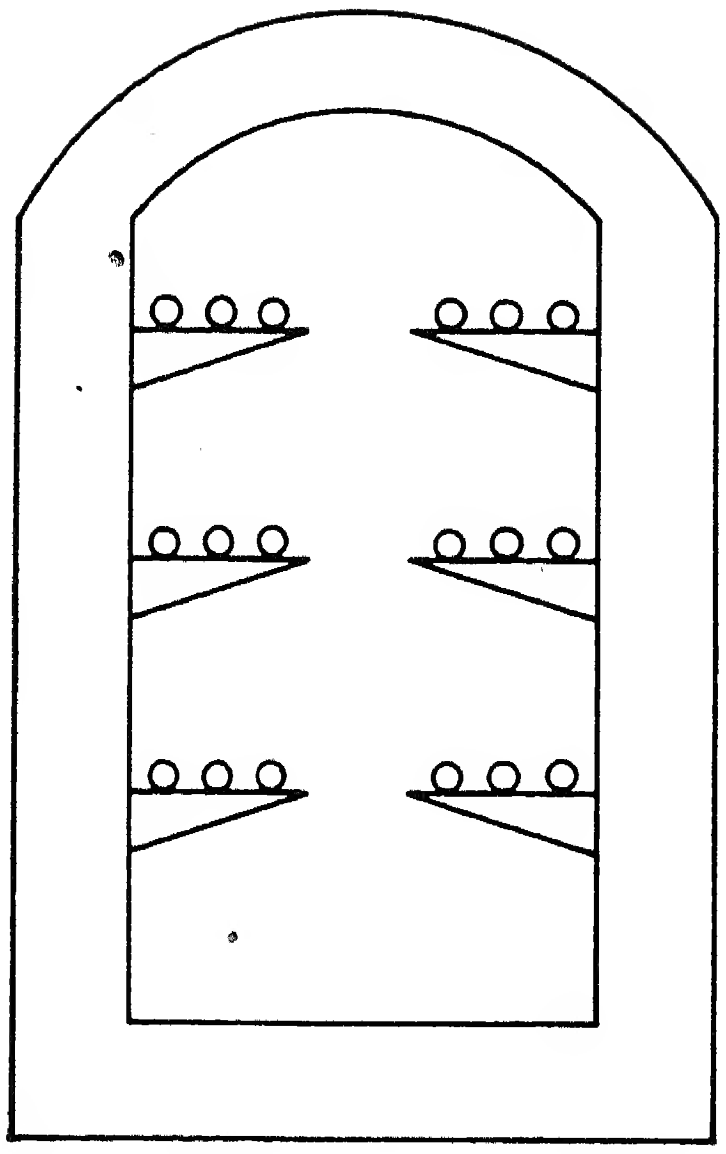


Figure 1 (left). Cable tunnel—cables mounted in racks in air

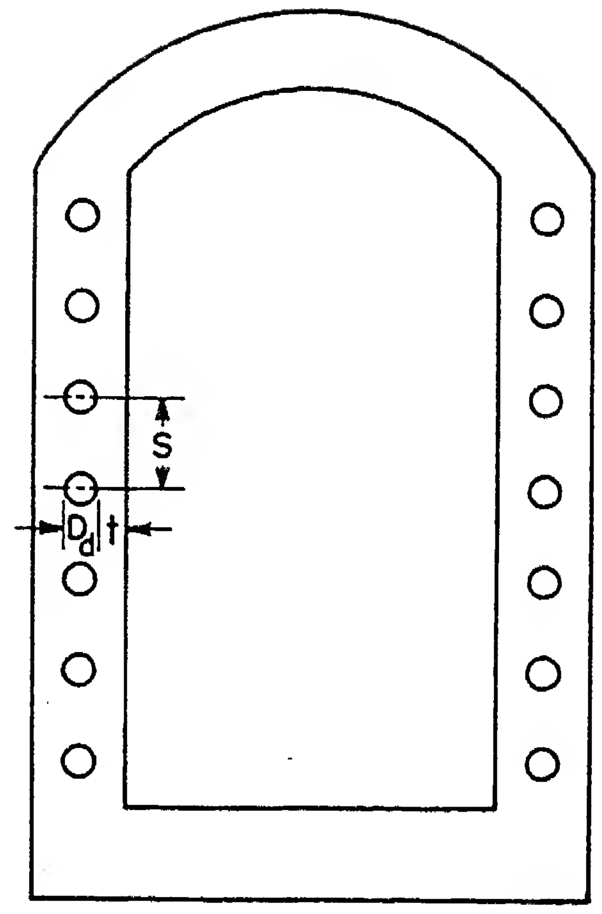


Figure 2 (right). Cable tunnel—cables installed in ducts in the tunnel wall

by saturating the soil with water, thereby improving its conductivity. While this practice has been considered, it is apt to be a very costly and difficult thing to do unless the hot spots are readily accessible, and unless there is available an adequate supply of cooling water in the neighborhood of the hot spots.

In the matter of radiation, it is theoretically possible to improve conditions by providing the cable with a matte black surface; it is well known that a shiny metallic surface is very bad from the radiation standpoint. However, there is some question as to how a matte black surface can be maintained in service. Fortunately cable surfaces usually do not remain shiny in service even if they leave the factory in that condition, but instead tend to become dull and oxidized. It is rarely practical to do anything about radiation beyond accepting the type of surface which exists in practice after some years of service, determining its radiation coefficient, and utilizing it in computing the cable heating.

Convection is another matter. While it is difficult to provide forced convection over a long cable line, it is obviously possible to blow air across a local hot spot if the cable is installed in free air and the hot spot is accessible. Furthermore it is possible to ventilate an entire tunnel, if cables are installed in tunnels. On the other hand, if cables are installed in ducts it is sometimes possible to cool the cables materially by blowing air or passing water through the loaded ducts.

In the case of pipe cable, the pressure medium (oil or gas) can be circulated and cooled. Another technique which has been proposed involves installing a copper pipe in the oil inside the cable pipe, and the circulation of water through this copper pipe. In the present paper, it is proposed to investigate some of these possibilities.

Forced-Air Cooling of Cable in Ducts

At least one large utility has developed a technique for blowing air through loaded cable ducts in power stations and substations, thereby materially reducing the cable temperature or, conversely, increasing its rating. This technique has been fully reported by R. W. Burrell, A. V. Falcone, and W. J. Roberts,[1] who have covered the matter exhaustively so there is no object in discussing it here. It is believed that while this technique is suitable for short duct runs, it may be difficult to apply to long duct runs due to the large amount of air necessary to remove the losses on long cable lines and to the pressure drop which would be involved.

The equation for T_1, given in their paper,[1] is identical with equation 2 of Appendix II. This is as it should be, since the case of a cable in a duct cooled by forced air is mathematically identical with the case of a cable installed in a ventilated tunnel.

Water-Cooling of Cables in Ducts

A report was presented on this subject to the Edison Electric Institute Transmission and Distribution Committee on October 10, 1942. This report deals with practical results attained and does not discuss theory.

Mathematically, this case is identical with the case of a pipe cable cooled by oil circulation (Appendix II), if the ducts are filled with circulating water. In general, water is the better cooling agent, and any leakage of water into the soil probably would be advantageous from the cooling standpoint, since soil thermal resistivity would be reduced thereby.

The practical problems of water supply, cost of cooling water, corrosion, and possibly erosion of the cable sheaths by such water, as well as problems of open-circuited sheath operation under such cooling conditions, present the most serious questions involved in this type of cooling. It has not been used much in the past.

Cables in Ventilated Tunnels, Mounted on Racks

Such cables, see Figure 1, are exposed directly to the cooling air, which affects the cooling of the cables favorably in three ways:

1. By breaking up natural convection currents, it prevents the lower cables from heating up the upper ones directly. Of course the heating of the cooling air by all the cables in the group increases the group ambient; but there is no direct transfer of heat by convection from the lower cables to the upper ones.

2. The moving air removes a good deal of heat directly from the cables, especially if its velocity is considerable. This heat otherwise would have to be dissipated through the earth surrounding the tunnel. Since this path for the flow of heat is still open, the effect of the cooling air is to provide an alternative parallel path thereby promoting heat removal.

3. If the velocity is sufficiently high, the flow of air will be turbulent, and there will be a tendency to break up the relatively thin film of air in the immediate neighborhood of the heated cables which contributes so materially to the surface thermal resistance of these cables. This scrubbing action reduces the temperature drop from the cable surface to the cooling air and is thus definitely beneficial.

The cooling air enters the tunnel at one end, flows longitudinally along the tunnel absorbing heat from the cable, and leaves the far end of the tunnel at an elevated temperature. On its way through the tunnel it also loses heat to the surrounding earth. The critical temperature rise

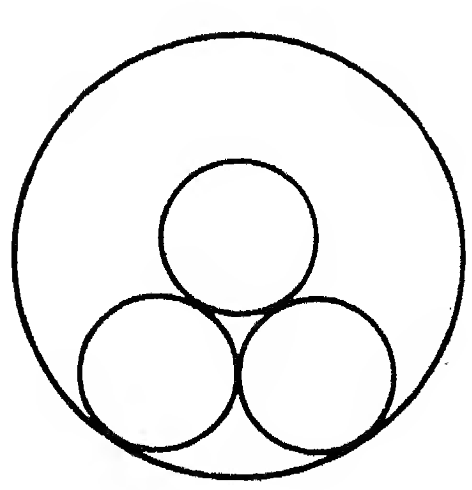
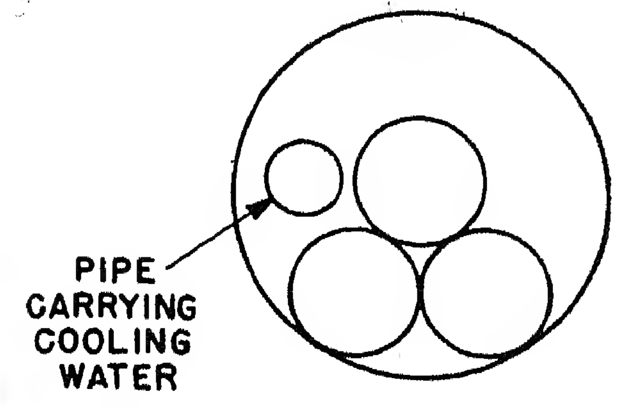


Figure 3 (left). High-pressure pipe-type cable—pressure medium circulated for cooling

Figure 4 (right). High-pressure pipe-type cable—cooling water carried in internal pipe



of the air is at the hot end of the tunnel. Equations for calculating this temperature rise, and also equations for calculating the cable loss which will give a specified tunnel temperature at the hot end, are developed in Appendix II.

Equations for calculating the surface thermal resistance of the cables are given in Appendix VII and are plotted in Figure 9. While no very complete data are available for the thermal resistance of cable surfaces cooled by air flowing longitudinally, if this air flows outside the cable surface, some light is thrown on this problem by Jakob;[2] and it has been correlated with the work done by McAdams[3] on air flowing transversely outside of cylinders and air flowing longitudinally inside of cylindrical tubes. This work indicates clearly that if formulas for the surface thermal resistance of tubes with air flowing inside them are used, they will give results which are no more than moderately conservative if the air flow is external instead of internal.

In this case, in addition to heat removal from the cable surface by forced convection, some heat will be transferred to the tunnel walls by radiation. In the equation presented in Appendix II allowance is made for this.

Cables in Ducts in a Tunnel Wall

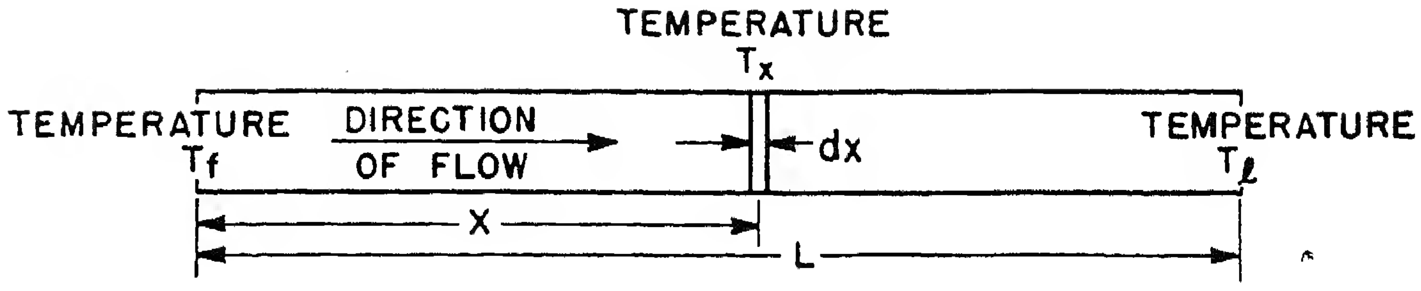
If the cables are installed in ducts in a tunnel wall (see Figure 2), conditions are not quite so favorable as they are when the cables are exposed directly to the cooling air. The cooling air does not have an opportunity to reduce the surface thermal resistance of the cables; furthermore a layer of concrete has been introduced between the cable duct and the cooling air. This concrete has a thermal resistance of its own; in addition, there is a thermal resistance between the surface of the concrete and the cooling air. See Appendix VI for equations, and Figure 8 for a plot, of these thermal resistances.

Equations for the temperature rise of the cooling air are developed in Appendix IV. In this case a portion of the heat developed by the cables is transmitted directly to the earth, and the remainder passes into the cooling air and is removed thereby.

Pipe Cable Cooled by Circulating the Fluid Which Is Used as a Pressure Medium

In this case (Figure 3) the pressure medium may be either oil or gas, which is circulated through the cable pipe and returned to the circulating pump through a

Figure 5. Illustrating air, gas, or oil flow through cable tunnel or cable pipe



return pipe. The fluid then is cooled by means of a heat exchanger and returned to the cable pipe for further circulation.

Mathematically this is analogous to the case of the cables mounted on racks in a ventilated tunnel. If the constants corresponding to gas under pressure or for oil are substituted in the equations in Appendix II, the temperature of the oil or gas at the hot end of the pipe can be computed. Appendix V gives suitable equations for surface thermal resistances, and Figure 6 gives a corresponding plot. When calculating the size of heat exchanger needed, allowance should be made for heat dissipation from the return pipe. Appendix III shows how this heat dissipation can be calculated.

As a practical matter, it has been found feasible to cool moderate lengths of pipe cable by this means, if oil is employed as a pressure medium. Gas is not so satisfactory as a cooling medium, and the cost and complexity of the compression equipment required for the circulation of the gas at the high operating pressure of 200 pounds or thereabouts makes cooling by gas circulation relatively unattractive. For the present it seems likely that this type of cooling will be confined to oil-filled pipe cables.

Cooling of Pipe Cables by an Internal Pipe Carrying Circulating Water

This type of cooling is mathematically analogous to the cooling of cables situated in ducts in a tunnel wall. The heat flows through two paths in parallel—one directly to the earth and one through a thermal resistance to the water in the cooling pipe. By using the proper constants, the equations in Appendix IV can be utilized to solve problems of this type.

The thermal resistance from the oil in the cable pipe to the water in the cooling pipe can be calculated from the equations given in Appendix V. Equations for calculating the thermal resistance from the cable surface to the oil in the pipe when oil circulation is used for cooling also are found in Appendix V.

Conclusions

Calculations based on the procedures given here show very clearly that for a relatively short length of pipe cable or lengths of tunnel it is possible to produce a material improvement in cable rating by forced ventilation, or forced circulation of oil, without going to circulating equipment so costly that the saving in cable cost is offset by the cost of the circulating equipment. Ventilation studies in tunnels up to about 1,500 feet long and oil circulation in pipes up to 3,000 feet long indicate that economies are feasible by using these methods. Doubtless, if circumstances warrant it, it may be feasible to cool even longer lengths economically by circulation and ventilation.

It would appear that the most efficient method of cooling cables in a ventilated tunnel is to mount them on racks, in direct contact with the cooling air. Such a construction is, however, not nearly so well protected against fire in event of a cable failure as is the alternative where cables are installed in ducts in the tunnel wall. Automatic safety devices, which cut off ventilation and operate a fire extinguisher when such trouble occurs, may prove to be a satisfactory answer to the fire problem.

It also would appear that circulation of the oil in a pipe cable is more effective as a cooling measure than water circulation in a small internal pipe. Moreover, oil is much more effective as a cooling medium, and more easily handled, than high pressure gas, when the pressure fluid is circulated. This is in sharp contrast to the condition which obtains when natural convection is the means of transfer from the cable to the pipe surface; here the gas is almost as effective as the oil, regarded as a cooling medium.

Circulation of air in loaded cable ducts seems to be a satisfactory solution where short duct lengths are involved; in the case of longer lengths, water circulation might be considered where the cost of the system is warranted, and where corrosion problems can be kept under control.

Appendix I. List of Symbols

W = total loss, watts per foot of pipe or tunnel

W_e = watts dissipated through the earth, per foot of pipe or tunnel

W_f = watts removed by air, oil, or water

T_c = conductor temperature, degrees centigrade

T_e = ambient, earth temperature, degrees centigrade

T_f = intake air, oil, or water temperature, degrees centigrade

T_x = air, water, or oil temperature at point x, degrees centigrade

T_x' = oil temperature at x when cooling with internal pipe; or, duct wall temperature at x when ducts are built into wall

T_1 = outlet air, water, or oil temperature, degrees centigrade

T_0 = oil temperature at end of return pipe, degrees centigrade

*Q = specific thermal capacity of air, water, or oil, watts second per cubic foot times degrees centigrade

v = air, water, or oil velocity, feet per second

A = clear area of tunnel, square feet

L = length of tunnel or cable pipe, feet

x = distance from intake end of cable pipe or tunnel, feet

R_e = soil thermal resistance, thermal ohms per foot (t.o.f.)

$R_e' = R_e + R_t$

R_i = thermal resistance of cable insulation, t.o.f.

R_s = thermal resistance of cable surface, t.o.f.

$R_p = \begin{cases} \text{(a) Thermal resistance from oil to water in cooling pipe, t.o.f.} \\ \text{(b) } R_c + R_t \end{cases}$

R_t = thermal resistance of inside wall of tunnel, or pipe, t.o.f.

R_c = thermal resistance of concrete between ducts and tunnel wall, t.o.f.

h = coefficient of heat transfer Btu per hour times square feet times degrees Fahrenheit

D = diameter, feet

D' = diameter, inches

D_c = 2.155 times outside diameter of cable, inches

D_d = diameter of duct in tunnel wall, inches

D_p = inside diameter of pipe, inches

D_p' = inside diameter of internal pipe, inches

D_p'' = outside diameter of internal pipe, inches

k = thermal conductivity of oil Btu per hour times square feet times degrees per Fahrenheit foot

G = mass velocity, pounds of fluid per hour times square feet of cross section

u' = viscosity, pound per hour times feet

C_p = specific heat at constant pressure, Btu per pound fluid times degrees Fahrenheit

S = free area between cables and pipe, square inches

u = viscosity, centipoises, of oil in pipe

u_w = viscosity, centipoises, of water

l = thickness of concrete between duct and inside of tunnel, inches

s = center to center spacing of ducts, inches

n = number of ducts

*Fluid	Value of Q
Air	31.0 (at atmospheric pressure and 50 degrees centigrade)
Oil	50,000
Water	117,000

Appendix II. Pipe Cooled by Circulating Oil or Ventilated Tunnel with Cable Supported on Racks or Troughs

Figure 5 shows the location of point x in the tunnel or pipe, and illustrates graphically the meaning of some of the symbols given in Appendix I.

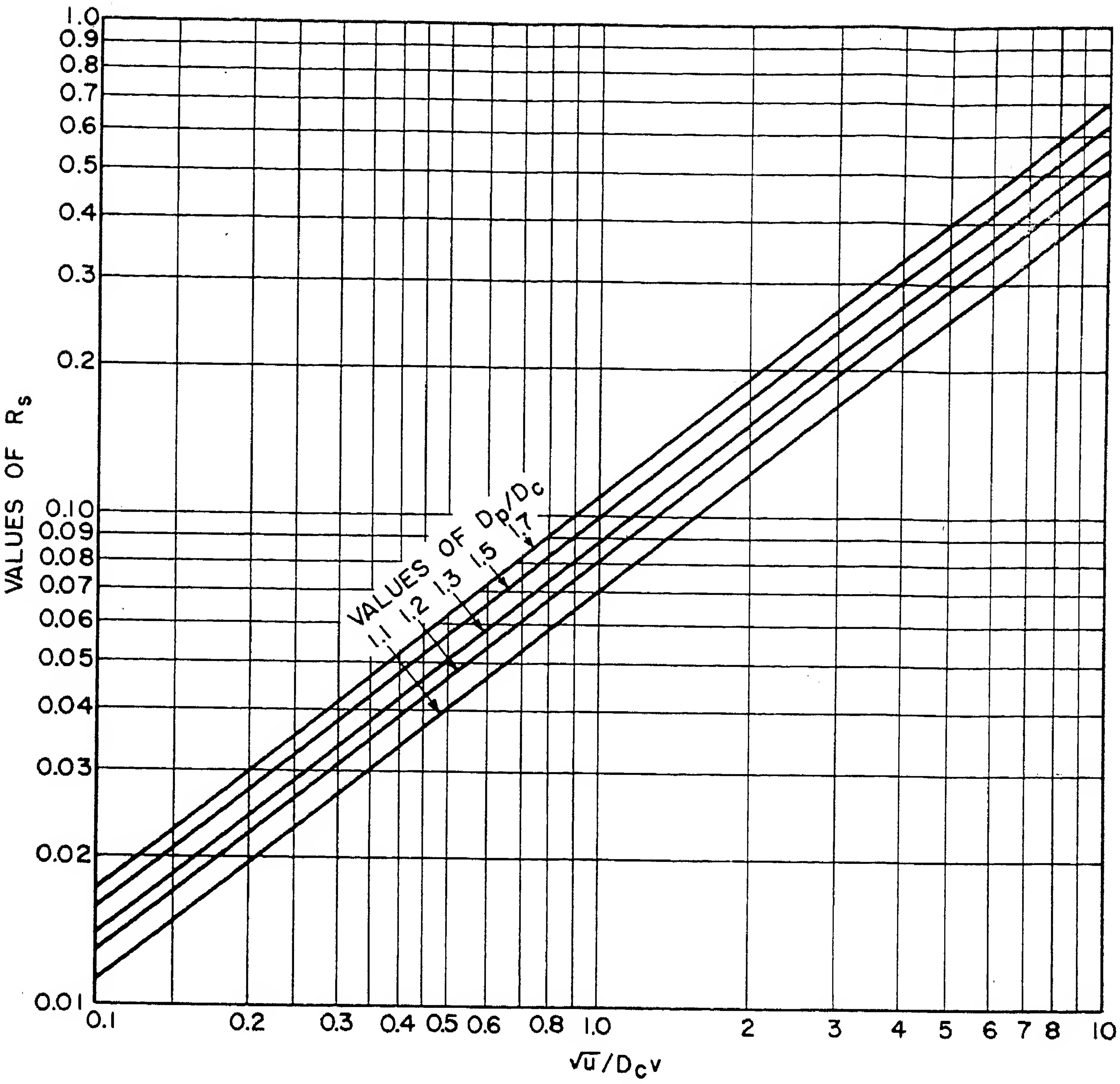


Figure 6. Values of R_s from equation 12

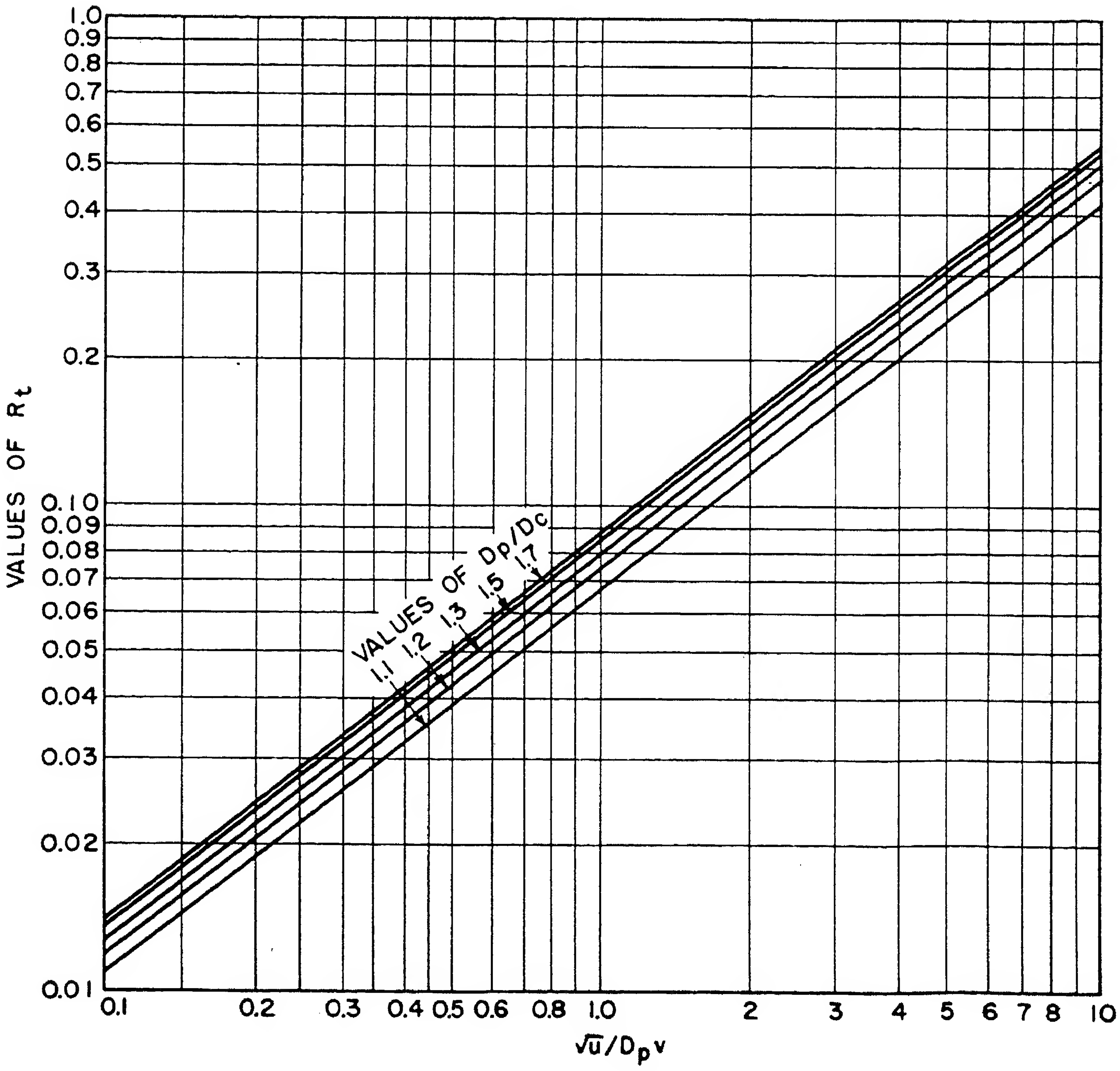


Figure 6(A). Values of R_t from equation 13

If it is desired the following equation may be used to obtain W

$$W = \frac{(T_c - T_e) - (T_f - T_e)\epsilon^{-L/QAvR_e'}}{R_i + R_s + R_e'(1 - \epsilon^{-L/QAvR_e'})} \quad (3)$$

Appendix III. Return Pipe

When oil is circulated outward through the cable pipe and flows back through an unheated return pipe, the heat dissipated to earth by the return pipe may be calculated as follows

In equation 2 write $T_f = T_1$, $T_1 = T_0$, $W = 0$

Then

$$T_0 = T_e(1 - \epsilon^{-L/QAvR_e'}) + T_1\epsilon^{-L/QAvR_e'} \quad (4)$$

$$\text{Heat dissipated} = QAv(T_1 - T_0)$$

$$= QAv(T_1 - T_e)(1 - \epsilon^{-L/QAvR_e'}) \quad (5)$$

Appendix IV. Pipe Cooled by Circulating Water in Internal Pipe or Ventilated Tunnel with Cables Installed in Ducts in the Tunnel Wall

This case differs from the one considered in Appendix II in that there is a thermal resistance R_p between the cooling medium and the cable surface, or cable oil. The heat is transmitted to the cooling medium and to the earth in parallel rather than through the cooling medium to the earth

$$T_x = T_x' - W_f R_p \quad (6)$$

$$W_f dx = W dx - \frac{T_x' - T_e}{R_e'} dx = QAv dT_x$$

$$W_f = W - \frac{T_x' - T_e}{R_e'} = QAv \frac{dT_x}{dx} \quad (7)$$

From equation 6

$$\frac{dT_x}{dx} = \frac{dT_x'}{dx} - R_p \frac{dW_f}{dx}$$

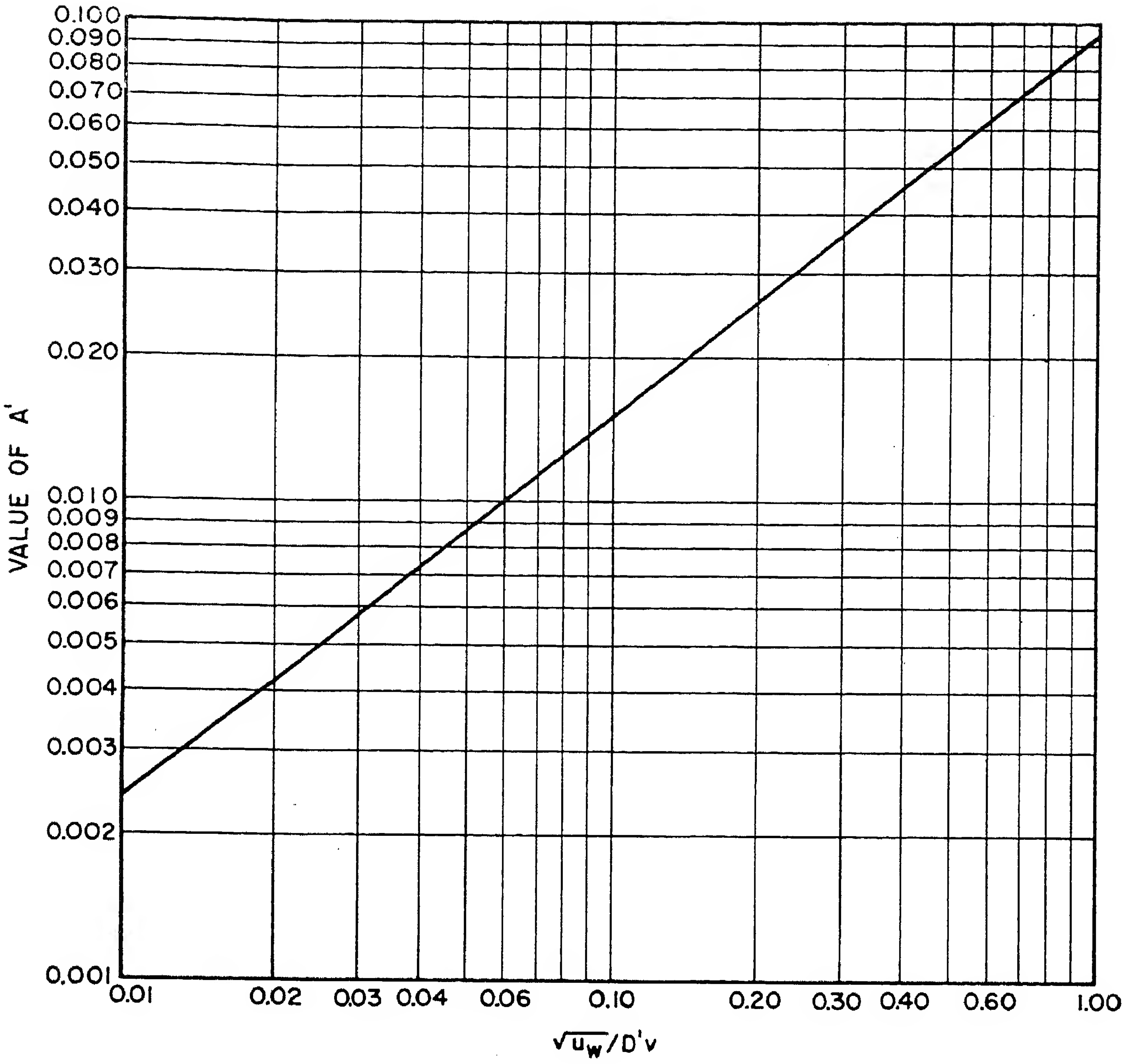


Figure 7. Values of A' in equation 14

Heat generated in length dx is Wdx

Heat dissipated to earth in length dx is $\frac{T_x - T_e}{R_e'} dx$

Heat removed by fluid in length dx is $QAvdT_x$

Then

$$Wdx = \frac{T_x - T_e}{R_e'} dx + QAvdT_x$$

whence

$$\frac{dT_x}{dx} + \frac{T_x}{QAvR_e'} = \frac{W}{QAv} + \frac{T_e}{QAvR_e'}$$

The solution of this differential equation is

$$T_x = (WR_e' + T_e) + C\epsilon^{-x/QAvR_e'}$$

At $x = 0$, $T = T_f$

so that

$$C = T_f - (WR_e' + T_e)$$

$$T_x = (WR_e' + T_e)(1 - \epsilon^{-x/QAvR_e'}) + T_f\epsilon^{-x/QAvR_e'} \quad (1)$$

$$T_1 = (WR_e' + T_e)(1 - \epsilon^{-L/QAvR_e'}) + T_f\epsilon^{-L/QAvR_e'} \quad (2)$$

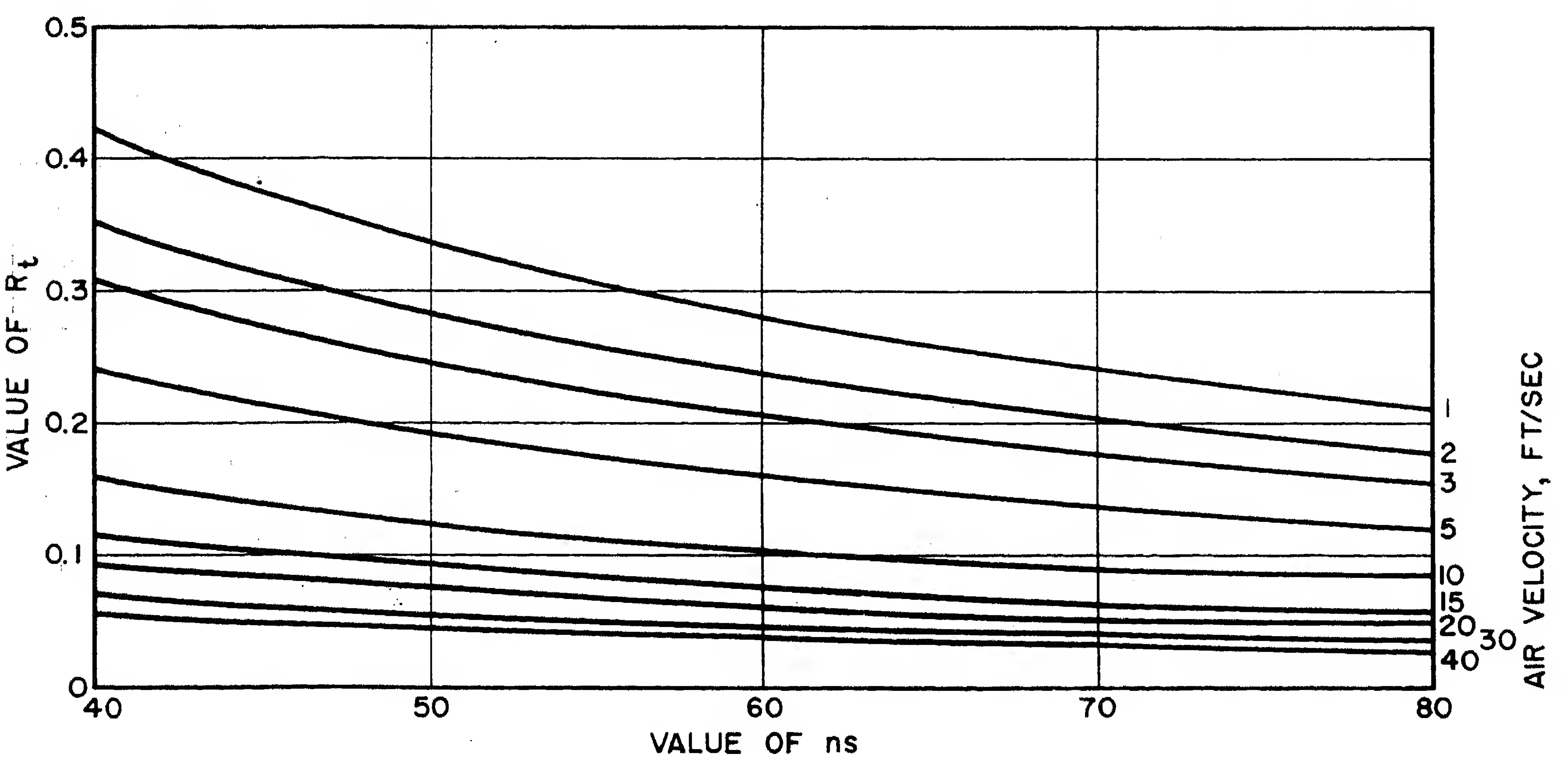
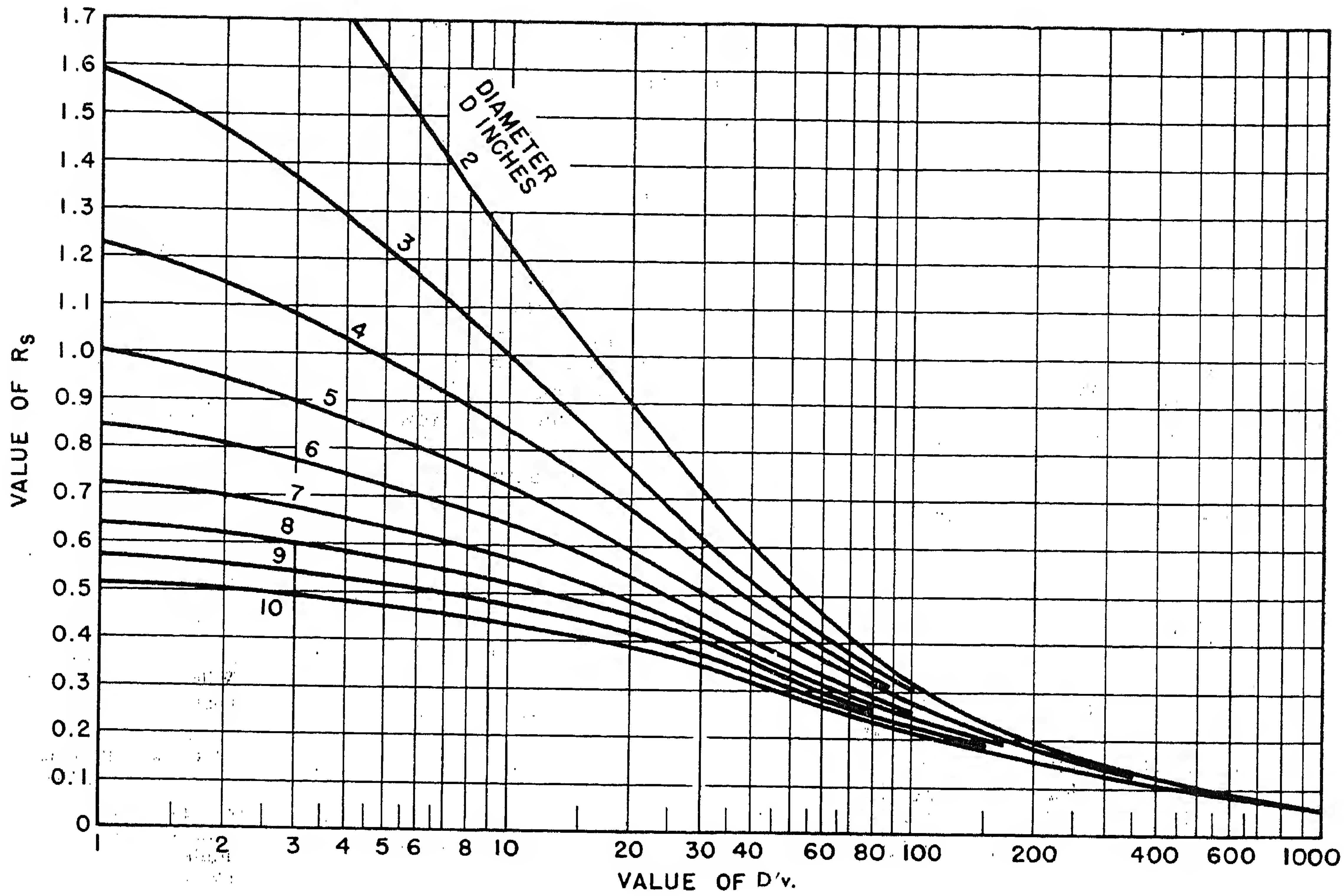


Figure 8. Value of R_t from equations 18 and 19

Figure 9. Value of R_s from equation 22



$$\frac{dW_f}{dx} = -\frac{1}{R_e'}\frac{dT_x'}{dx}$$

Therefore

$$\frac{dT_x}{dx} = \frac{dT_x'}{dx} + \frac{R_p}{R_e'}\frac{dT_x'}{dx} = \left(1+\frac{R_p}{R_e'}\right)\frac{dT_x'}{dx} \tag{8}$$

From equations 7 and 8

$$\frac{dT_x'}{dx} + \frac{T_x'}{QAv(R_e'+R_p)} = \frac{WR_e'+T_e}{QAv(R_e'+R_p)}$$

The solution of the equation is

$$T_x' = (WR_e'+T_e) + C\epsilon^{-x/QAv(R_e'+R_p)} \tag{9}$$

At $x=0$, $T_x = T_f$

$$T_x' = \frac{T_x + R_p(W+T_e/R_e')}{1+R_p/R_e'} = \frac{T_xR_e' + R_p(WR_e'+T_e)}{R_p+R_e'}$$

At $x=0$, $T_x' = WR_e'+T_e+C = T_0$ from equation 9

or $C = T_0 - (WR_e'+T_e)$

and

$$T_0 = \frac{T_fR_e' + R_p(WR_e'+T_e)}{R_p+R_e'}$$

$$T_x' = (WR_e'+T_e)(1-\epsilon^{-x/QAv(R_p+R_e')}) + \frac{T_fR_e'+R_p(WR_e'+T_e)}{R_p+R_e'}\epsilon^{-x/QAv(R_p+R_e')}$$

And at $x=L$

$$T_1' = (WR_e'+T_e)\times\left[1-\frac{R_e'}{R_p+R_e'}\epsilon^{-L/QAv(R_p+R_e')}\right] + \frac{T_fR_e'}{R_p+R_e'}\epsilon^{-L/QAv(R_p+R_e')} \tag{10}$$

Since

$$T_1 = T_1' + W(R_i+R_s)$$

$$W = \frac{(T_c-T_e)(R_p+R_e') - (T_f-T_e)R_e'\epsilon^{-L/QAv(R_p+R_e')}}{(R_i+R_s+R_e')(R_p+R_e') - R_e'^2\epsilon^{-L/QAv(R_p+R_e')}} \tag{11}$$

Appendix V. Coefficient of Heat Transfer—Moving Liquid in Pipe

Turbulent Flow

McAdams[3] on page 168, equation 4(C), gives

$$\frac{hD}{k} = 0.023\left(\frac{DG}{u'}\right)^{0.8}\left(\frac{C_pu'}{k}\right)^{0.4}$$

Flow will be turbulent if $DG/u' > 2{,}200$.

It is assumed that the three cables in the pipe are equivalent to an annular space, with inside diameter D_c, the diameter of the circumscribing circle around the three cables arranged in close triangular formation, and outside diameter D_p, which is the inside diameter of the pipe.

Following the technique described by McAdams[3] on page 200 for annular spaces, and substituting the constants for petroleum oils at 50 degrees centigrade (where these vary only slightly with temperature) and the more usual system of units, it is found that

$$R_s = \frac{0.0962u^{0.4}}{(D_cv)^{0.8}}\left[\left(\frac{D_p}{D_c}\right)^2 - 1\right]^{0.2} \tag{12}$$

$$R_t = \frac{0.0962u^{0.4}}{(D_cv)^{0.8}}\left[1-\left(\frac{D_c}{D_p}\right)^2\right]^{0.2} \tag{13}$$

Values of R_s and R_t are plotted in Figure 6 and Figure 6(A).

Viscous (Streamline) Flow

For this type of flow, there is little to be gained by using the relatively complex expressions available; it is better to neglect flow altogether, and use the technique based on natural convection and conduction developed by Buller & Neher.[4] For approximate work, the following simple equations can be used

$$R_s = \frac{0.74}{D_c} \qquad R_t = \frac{0.74}{D_p}$$

Internal Pipe

When heat is removed by water flowing in an internal pipe, the thermal resistance from oil to water in such a pipe is given by equation 14

$$R_p = \frac{0.0962u_w^{0.4}}{(D_p'v)^{0.8}} + \frac{0.74}{D_p''} = A' + \frac{0.74}{D_p''} \tag{14}$$

The second term in this equation is approximate. A better approximation can be obtained, if desired, by separating the heat flow to the water from the heat flow to the earth (using equation 14 as a basis) and then applying the Buller-Neher technique to re-evaluate this second term. (See Figure 4, reference 4). Values of A' are plotted in Figure 7.

Appendix VI. Heat Transfer from a Cable in a Duct in a Tunnel Wall to Moving Air in a Tunnel

Here the value of R_p is R_c+R_t where R_c is the thermal resistance of the concrete, and R_t that of the tunnel wall.

A simplified equation for R_c is

$$R_c=\frac{R_1R_2}{(R_1+R_2)n} \qquad (15)$$

where

$$R_1=3.3(0.215+2t/D_d) \qquad (16)$$

$$R_2=\frac{6.6(s+2t)}{s+D_d+4t} \qquad (17)$$

$$R_t=\frac{20.9}{ns(1+0.234v)} \text{ for velocities below 16 feet per second, and} \qquad (18)$$

$$=\frac{39.7}{nsv^{0.78}} \text{ for velocities above 16 feet per second} \qquad (19)$$

A good average value of R_2 is 3.66 (correct within $\mp 5\%$). Values of R_t are plotted in Figure 8.

If both side walls of the tunnel are heated, there will be a little direct radiation to the floor and roof. If all four sides are heated, there will be none. Radiation therefore should be neglected in calculating the heat flow from the tunnel wall to the ventilating air.

Appendix VII. Coefficient of Heat Transfer at Cable Surface Cable in Ventilated Tunnel

There do not seem to be available any completely satisfactory equations for cylinders cooled by forced convection with longitudinal flow. Jakob[2] states that for small wires in large tubes the coefficient of heat transfer approximates the same law as that for transverse flow, but has about 60 per cent of the numerical value of the transverse flow coefficient. However, the data on which this conclusion is based are based on low Reynolds numbers.

For Reynolds numbers of 2,000 to 16,000 McAdams[3] gives

$$\frac{hD}{k}=0.02\left(\frac{DG}{u'}\right)^{0.8} \text{ for longitudinal flow inside tubes} \qquad (20)$$

$$\frac{hD}{k}=0.24\left(\frac{DG}{u'}\right)^{0.6} \text{ for transverse flow outside tubes} \qquad (21)$$

By direct substitution in these equations, it is seen that the values given by equation 20 are less than 60 per cent of those given by equation 21 within the foregoing range. Accordingly equation 20 is recommended as being a little on the conservative side. Reducing equation 20 to more familiar units

$$R_s=\frac{14.3}{(D'v)^{0.8}}$$

Allowing for radiation this formula becomes

$$R_s=\frac{14.3}{2.66D'+(D'v)^{0.8}} \qquad (22)$$

Values of R_s are plotted in Figure 9.

References

1. Forced Air Cooling For Station Cables, **R. W. Burrell, A. J. Falcone, W. J. Roberts.** *AIEE Transactions*, volume 70, part II, 1951, pages 1798–1803.

2. Heat Transfer (book), **Max Jakob.** John Wiley and Sons, Inc., New York, N. Y., 1949, pages 558–9.

3. Heat Transmission (book), **W. H. McAdams.** McGraw Hill Book Company, New York, N. Y., second edition, 1942, pages 168, 170, 200, 222.

4. The Thermal Resistance Between Cables and a Surrounding Pipe Duct Wall, **F. H. Buller, J. H. Neher.** *AIEE Transactions*, volume 69, part I, 1950, pages 342–49.

Discussion

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): The practical value of this paper would be enhanced considerably if numerical examples based on actual or assumed conditions were included. To save space, the detailed mathematical steps could be omitted, but the given conditions and resultant ratings should be included. In addition to assisting the reader to apply this method of calculation, these examples also would enable him to evaluate the efficacy of artificial cooling.

F. H. Buller: Mr. Hickernell's point is well taken. A numerical example is often helpful in clarifying the use of equations, but it was feared that the addition of a completely worked example might make the paper too long. However, in deference to Mr. Hickernell's point of view, which is doubtless shared by many others, the following example is given herewith. This example not only shows that a moderate circulation of oil will cut the temperature rise of the conductor substantially in half but illustrates the proper use of equations 2 and 10 in the paper for a pipe cable system when the watts loss is divided between the conductor and the pipe.

Effect of Oil Circulation on Pipe Cable Temperature

Cable 1,500,000-circular-mil conductor compact segmental
Insulation 0.560 inch
Outside diameter conductor 2.750 inches
Inside diameter pipe 8.125 inches $=D_p$
Outside diameter pipe 8.625 inches
Depth of burial 36 inches
Soil thermal resistivity 80 degrees centigrade centimeters per watt
Watts on conductor 21.0
Watts on shield 21.6
Watts on pipe 27.0
Clear area of pipe 0.237 square foot $=A$
Oil velocity (assumed) 1.0 foot per second
Oil temperature at inlet 30 degrees centigrade $=T_f$
Earth ambient temperature 25 degrees centigrade $=T_e$
Length of pipe 2,000 feet

Thermal Resistances

Insulation thermal resistance $=R_i=0.57$ thermal ohm per foot
Surface thermal resistance, cable to pipe with no circulation $=0.30$ thermal ohm per foot (see reference 4, Figure 4)
Soil thermal resistance $=R_e=1.17$ thermal ohms per foot

(A) Conductor Temperature, No Circulation

Rise of conductor over shield	$21.0\times0.57=$	12.0 deg C
Rise of shield over pipe	$21.6\times0.30=$	6.5 deg C
Rise of pipe over earth	$27.0\times1.17=$	31.6 deg C
Earth ambient		25.0 deg C
Total		75.1 deg C

(B) Conductor Temperature with Oil Circulation at 1.0 Foot per Second Velocity

Reynolds number $Dvp/\mu=197$; hence, flow is viscous. Use Buller-Neher method of determining R_s+R_t

Assume average oil temperature $T_m=33$ degrees centigrade

Then $(WD_c{}^3T_m{}^3)^{1/4}=201$, $R_s+R_t=0.245$ (see reference 4, Figure 4)

This quantity may be divided between R_s and R_t in inverse proportion to D_c and D_p, by using the equation

$$R_s+R_t=\frac{P}{D_c}+\frac{P}{D_p}=0.245$$

giving $R_s=0.142$, and $R_p=0.013$.

While this procedure is not rigorous, it is accurate enough for all practical purposes. *Note:* If R_t were taken as 0.245 instead of 0.103, T_1 would work out to be 32.88 degrees centigrade instead of 32.85. Thus the method of obtaining R_s and R_t given here is perfectly satisfactory from the practical standpoint.)

Then

$$R_e'=R_e+R_t=1.27$$

$$\frac{L}{QAvR_e'}=\frac{2000}{50000\times0.237\times1.0\times1.27}=0.133 \text{ and } \epsilon^{-0.133}=0.874$$

From equation 2

$$T_1=(21.6\times1.27+25)(1-0.874)+30\times0.874=32.85 \text{ degrees centigrade}$$

Effect of Watts on Pipe on Oil Temperature

To determine the effect of the watts on the pipe on the oil temperature, use equation 10, substituting R_t for R_p, and R_e for R_e' in that equation.

Watts on pipe $=27.0-21.6=5.4$

$$\frac{R_e'}{R_p+R_e'} \text{ becomes } \frac{R}{R_e'}-\frac{1.17}{1.27}=0.922$$

$$T_1=(5.4\times1.17+25)(1-0.922\times0.874)+30\times0.922\times0.874=30.3 \text{ degrees centigrade}$$

Rise of oil due to watts on pipe $=30.3-3.30=0.3$ degree centigrade

Total oil temperature	=32.9+0.3	=33.2 deg C
Rise of shield over oil	=21.6×0.14	=3.0 deg C
Rise of conductor over shield		=12.0 deg C
Total		48.2 deg C

(*Note:* The original assumption of 33 degrees centigrade for the average oil temperature therefore is justified. If this figure had differed more widely from 33 degrees centigrade, the calculation would have to have been repeated, using a different assumed oil temperature.)

Thus, if the oil in the pipe is used as a circulating and cooling medium, with an inlet oil temperature of 30.0 degrees centigrade, and a velocity of flow of 1.0 foot per second, the conductor temperature will be reduced from 75.1 to 48.2 degrees centigrade, that is, the temperature rise over the earth ambient is just about cut in half, in this particular case.

Lightning to the Empire State Building—Part III

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Synopsis: Previous papers[1,2] have reported lightning investigations conducted at the Empire State Building in New York City during the years 1935 through 1940. During 1941, 1947, 1948, and 1949 further data were obtained on all lightning stroke characteristics. This paper presents a summary of all data obtained during the 10 years in the form of frequency curves.

1. The majority of the strokes at the Empire State Building start with an upward stepped leader followed by a continuous current discharge with an average amplitude of 250 amperes. For about 50 per cent of the strokes this discharge mechanism is followed by one or more current peaks (average 2.3) of much higher amplitude. The current peaks are initiated by downward continuous leaders.

2. Proof has been obtained of the existence of continuous current by oscillographic and photographic methods.

3. Fifty per cent of the strokes have a total charge in excess of 19 coulombs. The maximum measured was 164 coulombs.

4. The maximum stroke duration was 1.5 seconds while 50 per cent of the strokes were in excess of 0.27 second.

5. The wave shapes, front and tail, and current crests for 80 current peaks were determined.

6. Rates of rise of current peaks frequently are 10 to 20 kiloamperes per microsecond but may be as high as 50 kiloamperes per microsecond.

7. Good agreement is shown with data by other investigators.

8. The application of the data to transmission line problems is discussed.

Terminology

THE terminology to be used in this paper is the same as used by K. B. McEachron in his paper on the same subject.[2]

A few of the principal terms are:

Stroke: The complete lightning phenomenon consisting of one or more discharges following essentially the same path with a duration of the order of relatively large fractions of a second.

Current Peak: A momentary rapid increase of current within a stroke with a duration of the order of microseconds.

Continuous Current: A lightning discharge of relatively low amplitude but of duration of large fractions of a second.

Multiple Stroke: A lightning stroke having more than one current peak.

Equipment

The equipment used during the prewar years including 1941 has been described elsewhere.[3–5] When the investigation was continued after the war, some modifications were made to accommodate changed conditions. In 1947 the antenna structure of the National Broadcasting Company on top of the building was changed. This required 69 feet of cable between the bushing and lightning rod above the antenna and the oscillograph located under the roof of the building. A high-pressure gas-filled cable was used. The bushing on top of the antenna was made of Herkolite.

The automatic cathode-ray oscillographs (CRO), see Figure 1, were redesigned with a complete vacuum-tube system. As before, a high-speed CRO with logarithmic sweep of about 100 microseconds was used to record the fast components or so-called high current peaks of the lightning stroke. The low-speed CRO was equipped with two CRO tubes for simultaneous recording of the continuing current components of much

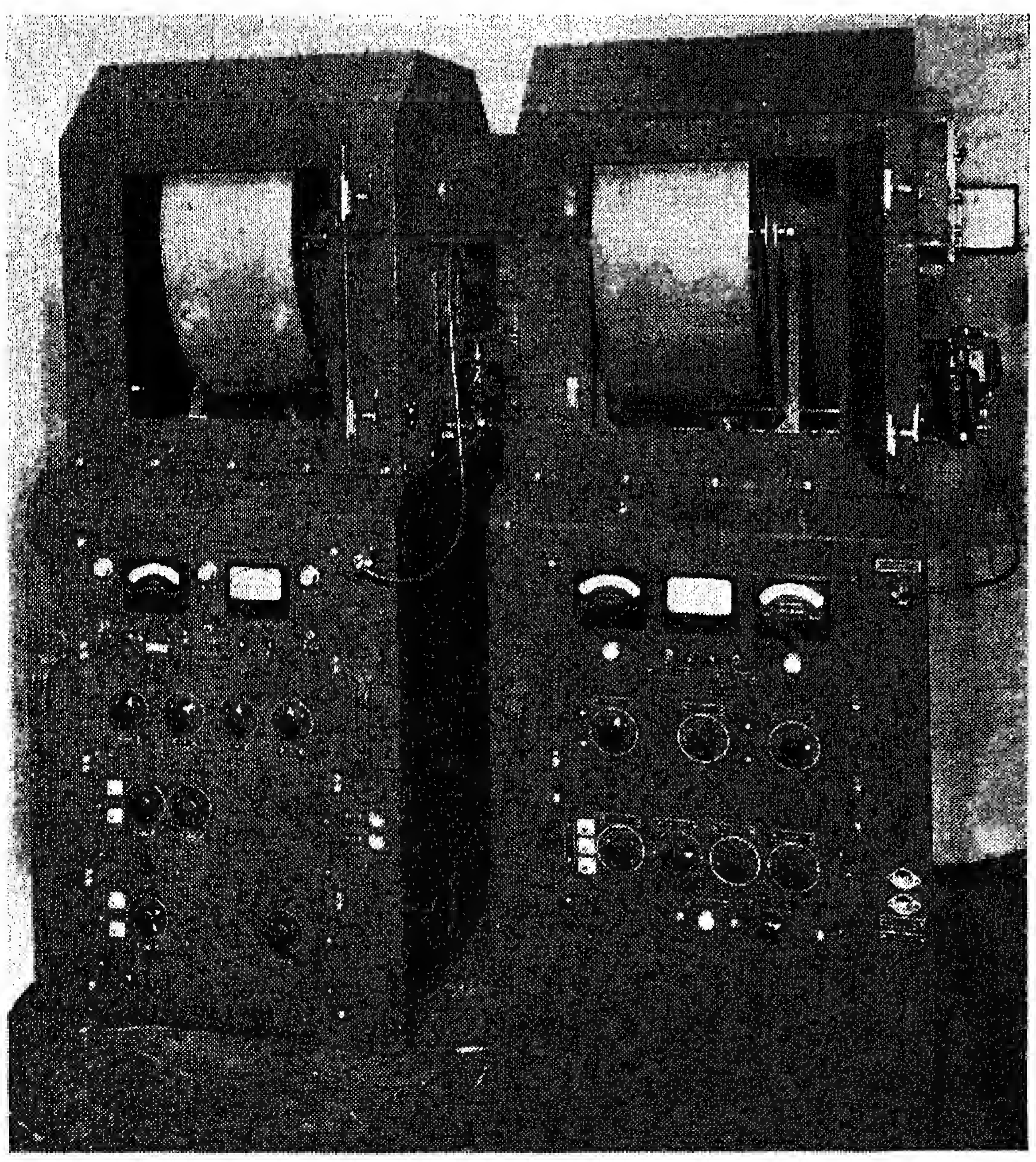

Figure 1. Front view of oscillographic equipment used at the Empire State Building. Low-speed oscillograph at left and high-speed oscillograph at right. Film drum housings open to show internal construction

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The authors wish to express their appreciation to Dr. K. B. McEachron who directed this work; the National Broadcasting Company for the use of their antenna and space for the oscillographs, in particular Mr. R. M. Morris and Mr. T. J. Buzalski; the United States Weather Bureau and the Public Service Company of New Jersey for storm warnings; and the large number of the authors' associates who co-operated in obtaining and analyzing the data.

Table I. Lightning Statistics at Empire State Building*

	Year										
	1935	1936	1937	1938	1939	1940	1941	1947	1948	1949	Total
Storm days	27	26	28	39	29	35	26	21	24	20	275
Strokes to Empire State Bldg.	5	22	41	24	3	20	6	48†	21	36	226
Stroke days at Empire State Bldg.	4	4	6	6	1	5	4	13	5	9	57

* Covers period of observation and does not give the complete number of storm days in New York City.
† Two of these strokes are believed to have hit the building below the lightning rod.

lower amplitude than the current peaks. These two tubes had different sensitivities and covered a current range of 2,000 amperes to a minimum of 10 amperes for a time range of 0.7 second. Another radical change was made during 1947 through 1949 by using a constant resistance noninductive shunt of 0.01 ohm. In order to adjust the wide variation in current amplitudes existing in the lightning strokes (100,000 amperes to zero ampere) to the CRO sensitivity, Thyrite suppression networks were used at the oscillographs. These were adjusted to give deflection scales similar to those used in the previous investigations. This arrangement has the advantage that the shunt proper can be made less inductive and measurements of wave fronts will be more accurate.

A further improvement in ease of operation was the installation of automatic starting equipment for power supplies of the CRO's. This was accomplished by utilizing the corona current flowing through a small probe installed on the roof of the building. At a predetermined corona intensity the equipment would be energized. The equipment would shut down automatically 1/2 hour after corona had been lowered below the critical corona level. Of course once the power supplies were energized the beam and sweep of CRO's were initiated by the lightning stroke.

Two or three different types of cameras[1–5] were installed at 500 Fifth Avenue at a distance of 2,550 feet from the top of the Empire State Building between 1935 and 1941, and at 521 Fifth Avenue at a distance of 2,700 feet from 1947 through 1949, to photograph the same lightning strokes that were recorded oscillographically.

Results of the 10-Year Investigation

Strokes to Empire State Building

It is of some interest to compare the number of strokes to the Empire State Building with the number of storm days in New York City. Both figures are given in Table I and comprise the period during which the building was under observation, usually from the end of May to the end of September. The number of storm days and number of strokes, therefore, are not total values. However, the comparison is valid for the periods of investigation. This 10-year period should be of sufficient length to establish valid lightning records to the building. The average number of storm days was 27.5. This checks quite closely with the isokeraunic charts published by W. H. Alexander.[6] The average number of strokes to the building were 22.6 per year and it was struck about 0.82 time per storm day. Of great interest is the relatively large change in number of strokes per year—maximum to minimum is about 16 to 1—as compared to the change in storm days—maximum to minimum is about 1.75. Table I also shows the number of stroke days which gives the number of days during which the building was struck during any one year. This is a relatively smaller fraction of the storm days, about 0.21. Comparing stroke days with strokes to the building, it is at once evident that a large number of strokes (about four) occur per stroke day.

Polarity of the Strokes

For the entire period of the investigation, a total of 80 stroke oscillograms were obtained from which polarity could be determined; 86 per cent of the strokes were entirely negative; 14 per cent were both negative and positive. In no case was a stroke entirely positive. Of the strokes with mixed polarity, the greater proportion of charge was negative. In most of these cases the positive charge was contained in the continuing portion of the stroke.

Stroke Charge

Of the 73 strokes that could be evaluated for stroke charge, 50 per cent have a charge in excess of 19 coulombs. This figure is slightly less than the previously obtained value of 25 coulombs. The maximum measured was 164 coulombs. These values are shown in Figure 2.

Continuing Strokes—General

The continuing type of current discharge in a lightning stroke supplies approximately 95 per cent of the total charge. This percentage might be still greater if the sensitivity of the measuring equipment were greater. Of 135 strokes measured with oscillograph and camera, about 50 per cent had no current peak but had a continuing type of current only. This undoubtedly is a characteristic of the Empire State Building and other very tall structures. It probably results because of the initial upward leader which apparently can occur before charges in the cloud are sufficiently concentrated to produce a downward leader followed by a current peak from the building. Another reason may be that the charges available in the cloud under such conditions are limited and in a case of a stroke to ordinary terrain would have resulted in a cur-

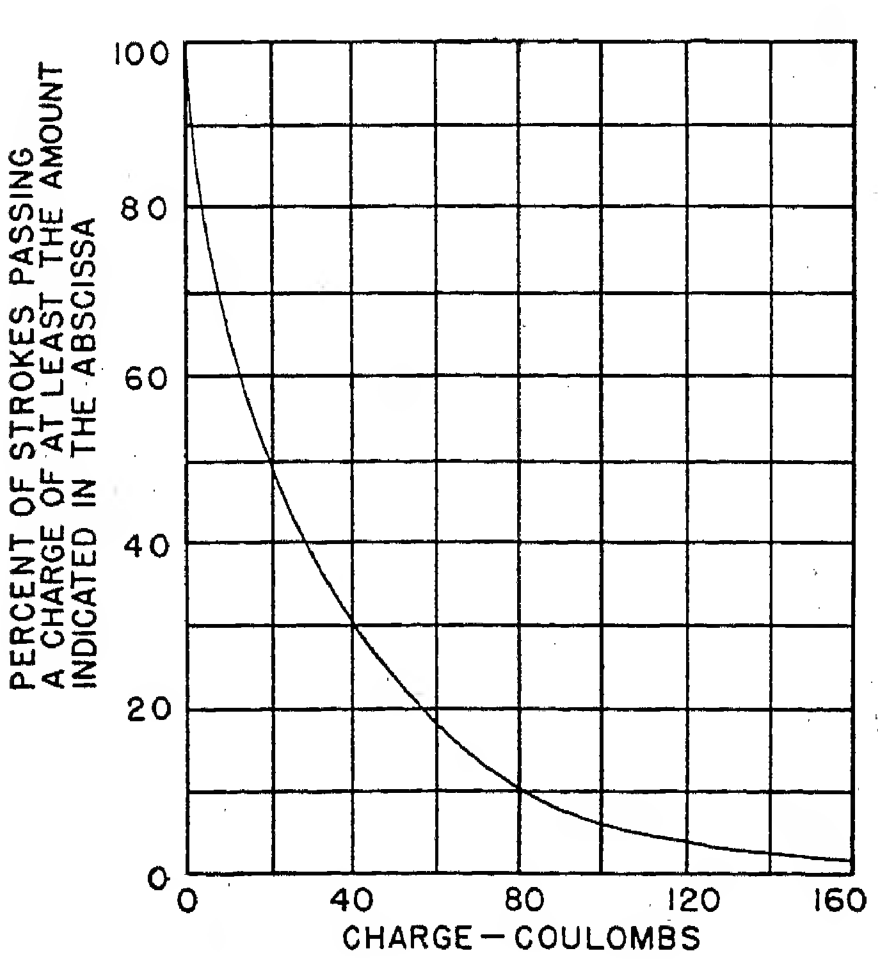


Figure 2. Frequency of occurrence of stroke charge based largely on low-speed oscillographic data. Based on 73 strokes

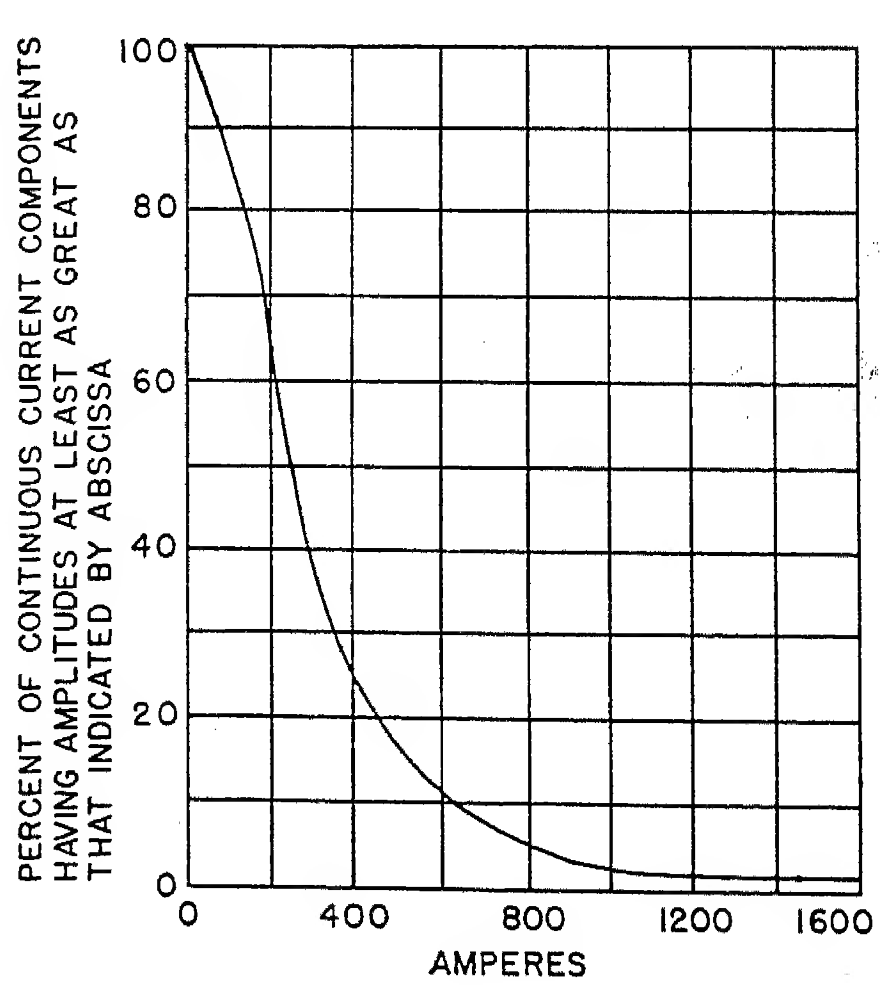
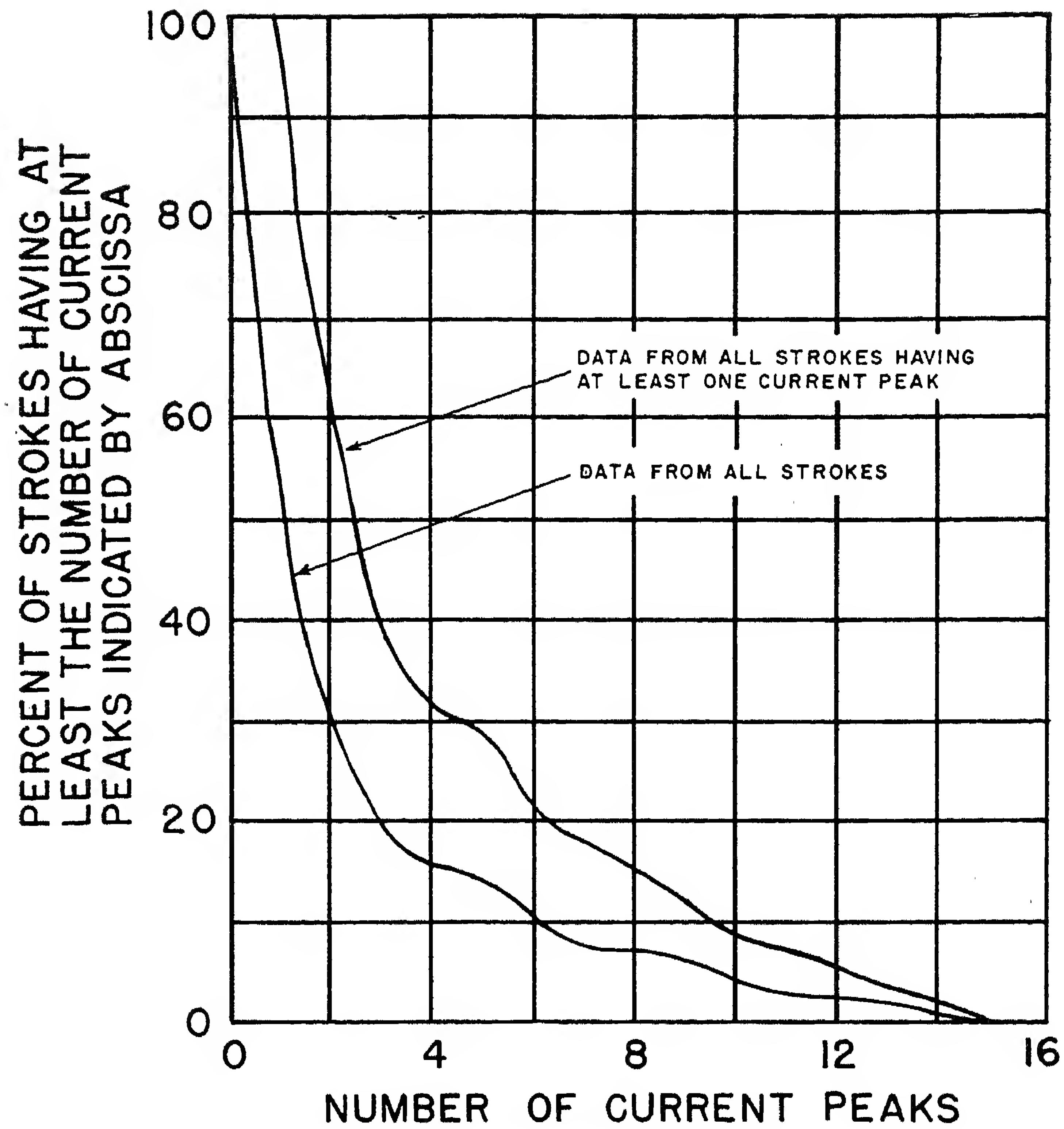


Figure 3. Frequency of occurrence of maximum amplitudes of continuous current components in lightning strokes to the Empire State Building. Based on 64 strokes

rent peak and subsequent continuing current of short duration. In the case of the building, the relatively small charges are drained off by the continuing current, eliminating the possibility of a current peak.

Figure 4. Frequency of occurrence of current peaks in lightning strokes to the Empire State Building. Based on 135 strokes measured either photographically or oscillographically



Continuing Strokes—Amplitude

It is difficult to present amplitude values for continuing strokes, since they do not have a wave shape as well defined as the current peaks, see Figure 2 in K. B. McEachron's paper.[2] However, it appeared of interest to report some data concerning the distribution of maximum amplitude per stroke. These values are shown in Figure 3 and show that the maximum current is of the order of 1,450 amperes. Fifty per cent of the continuous current components had a maximum amplitude of more than 250 amperes.

Number of Current Peaks Per Stroke

Because of the fact that some strokes to the building have no current peaks, the frequency curves for this study can be started with zero current peaks, as shown on Figure 4. Fifty per cent of the strokes fall into this classification. Obviously, zero does not mean that there was no discharge but that the discharge had a continuing current component only. Another curve on Figure 4 shows the number of current peaks for those strokes which contained one or more current peaks. This latter curve checks very well with data of other investigations.[7] Fifty per cent of such strokes had between two and three current peaks. Fourteen current peaks were the maximum number recorded in one stroke.

Stroke Duration

These statistics are shown on Figure 5. Fifty per cent of the strokes have a duration exceeding 0.27 second. The maximum recorded duration of 1.5 seconds occurred in 1937 and has never been exceeded. Whenever photographs were obtained simultaneously with the oscillograms, the photographic trace showed a longer total duration than the oscillogram. For example, the duration of 50 per cent of the strokes based on 50 strokes is 0.3 second from oscillograms and based on 19 strokes 0.47 second from photographs.

Current Peaks—Crest Current

Figure 6 shows amplitudes of 84 current peaks. All but two of these were of negative polarity. Fifty per cent of the peaks exceeded 10,000 amperes. The maximum current recorded was 58,000 amperes and was of positive polarity. Other data obtained from direct strokes[8] indicate that 50 per cent of the strokes exceeded 5.5 kiloamperes, while investigations on transmission lines[9,10] indicate that 50 per cent of the tower currents exceeded 10 kiloamperes.

Current Peaks—Duration

Surge current durations are expressed in time to half value. Figure 7 shows that of 82 current peaks measured, 50 per cent exceeded 34 microseconds to half value, while 20 per cent were 68 microseconds and longer. The maximum duration is not known exactly because in a few cases the half value was not reached at the end of the sweep, but it is estimated to be 120 microseconds.

Current Peaks—Charge

For purposes of identifying charges produced in current peaks as compared to charges carried by continuing currents, only the charge to half value of the current surge has been measured from the oscillogram records, and the frequency of distribution is shown on Figure 8. Fifty per cent of the 83 current peaks had a charge exceeding 0.15 coulomb, 6 per cent exceeded 1.7 coulombs and the maximum

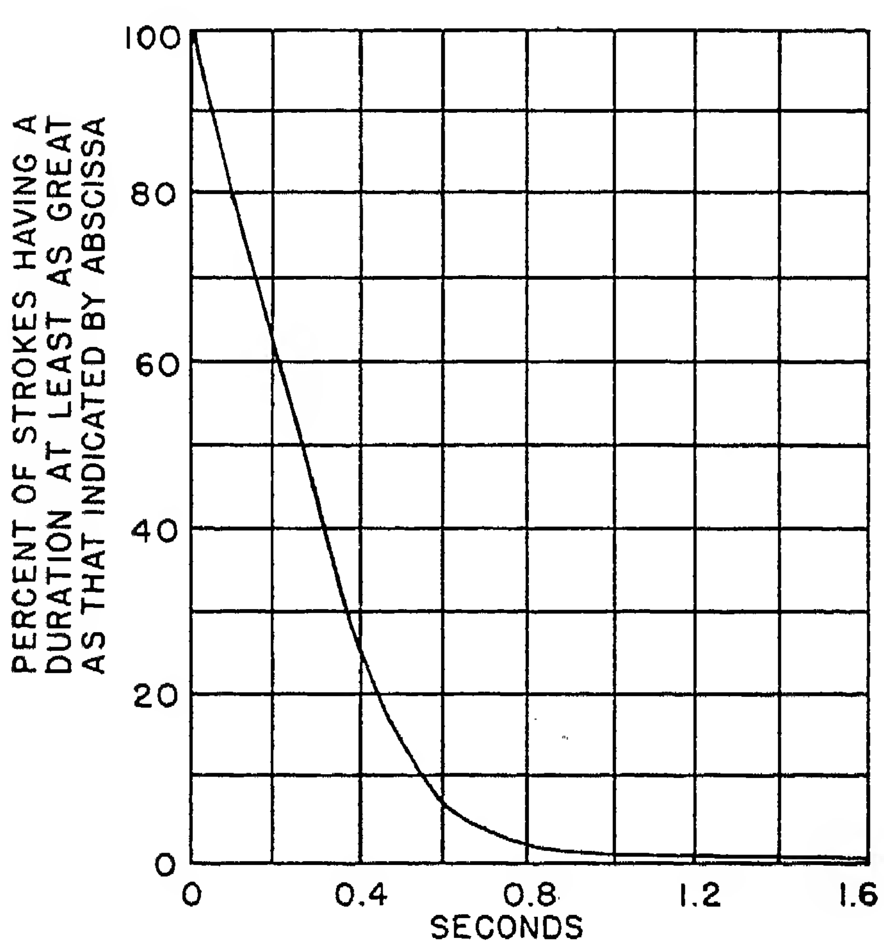


Figure 5. Duration of strokes plotted as a function of frequency of occurrence. Based on 76 strokes

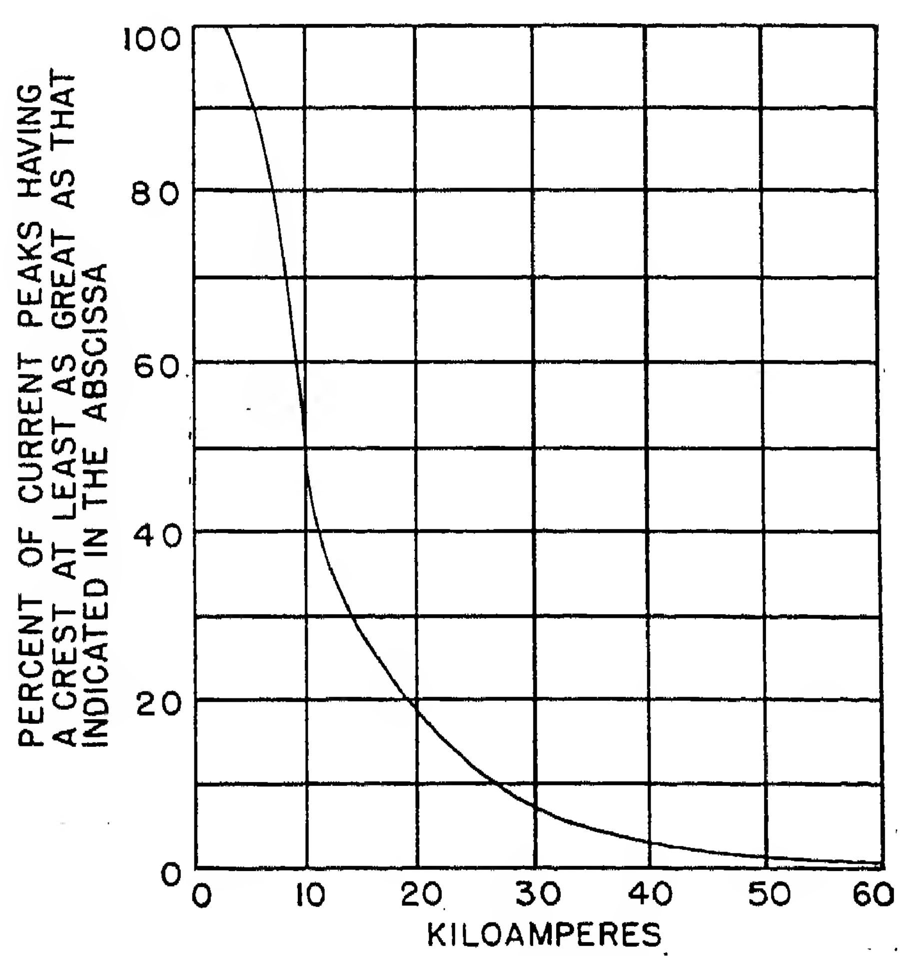


Figure 6. Maximum amplitude of current peaks as a function of frequency of occurrence. Based on 84 current peaks

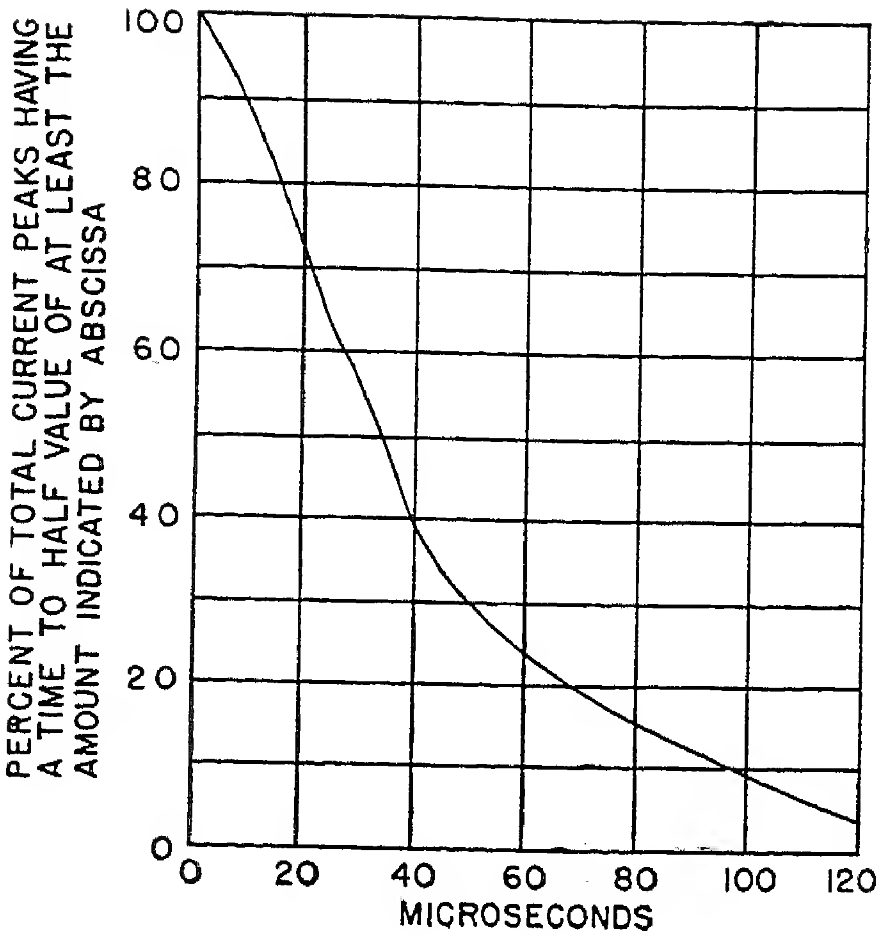


Figure 7. Duration of current peaks measured to half value as a function of frequency of occurrence. Based on 82 current peaks

charge recorded was 4.9 coulombs produced by the 58,000 ampere current peak of positive polarity.

Current Peaks—Wave Front

Eighty-two current peaks indicated that 50 per cent had a time to first crest of 1 microsecond or more; 38 per cent, 1.5 microseconds or more. In general, the wave fronts did not exceed 8.3 microseconds, see Figure 9.

There were, however, two current peaks with very much longer fronts, 30 and 85 microseconds respectively. These peaks were the third and eleventh peak in two multiple strokes.

Current Peaks—Rate of Rise

From 71 current peaks, the effective rate of rise, measured through the 10-per-cent and 90-per-cent points on the wave front, could be obtained. Figure 10 shows that 50 per cent of the strokes had a rate of rise in excess of 12.5 kiloamperes per microsecond. The maximum is of the order of 50 kiloamperes per microsecond for a peak of 30 kiloamperes. The highest values of rate of rise are usually associated with current peaks of relatively low amplitude of the order of a few thousand amperes. However, some of the higher current peaks of 30- to 50-kiloamperes crest have had a rate of rise of 20 kiloamperes per microsecond or more.

Polarity of Current Peaks

Five positive current peaks were recorded occurring during otherwise negative strokes. One of these had the highest current peak, 58,000 amperes, and is shown in Figure 3 of K. B. McEachron's paper.[2] Another was the eleventh current peak of stroke number 28 in 1947. It had a very slow front—crest reached at a time later than 80 microseconds with

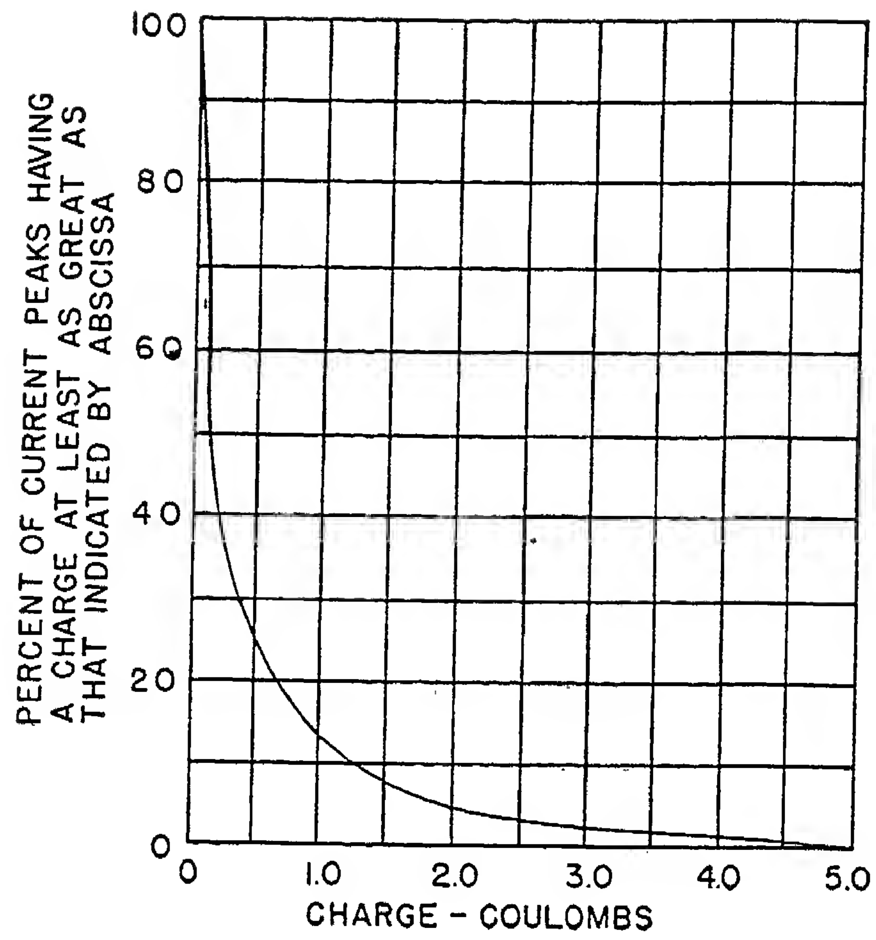


Figure 8. Charge of current peaks as a function of frequency of occurrence. Coulomb values based on time to half value of crest current. Based on 83 current peaks

a crest current of 12,000 amperes and a charge of 0.63 coulomb to 80 microseconds. This current peak is similar to peak number 3 of Figure 3 of the same paper. No detailed data are available on the other three current peaks. Of the current peaks for which polarity data are available, 94 per cent were negative.

Intermediate Current Component

In many instances the current peaks with a duration of tens of microseconds are followed by a component decaying in hundreds of microseconds, which in turn is followed by a continuing current component. This type of wave is shown in Figure 11. Records for 1 year have been evaluated for wave durations of this intermediate-type current component and are shown in Figure 12. The duration is given in terms of the time to half value of the exponential tail into which most of these surge shapes can be resolved. The amplitudes of these surges were of the order of 2,500 amperes and less. A few had amplitudes in excess of this value.

Accuracy of Measurement

The average accuracy of oscillographic measurements on current peaks is of the order of ± 20 per cent. This applies to current amplitude, rate of rise, charge, wave fronts, and wave tails. For extreme conditions, for instance, very short fronts, very low current amplitudes, and so forth, the accuracy may be of the order of ± 50 per cent.

Discussion of Results

Principle Contribution

This 10-year investigation of lightning at the Empire State Building has yielded

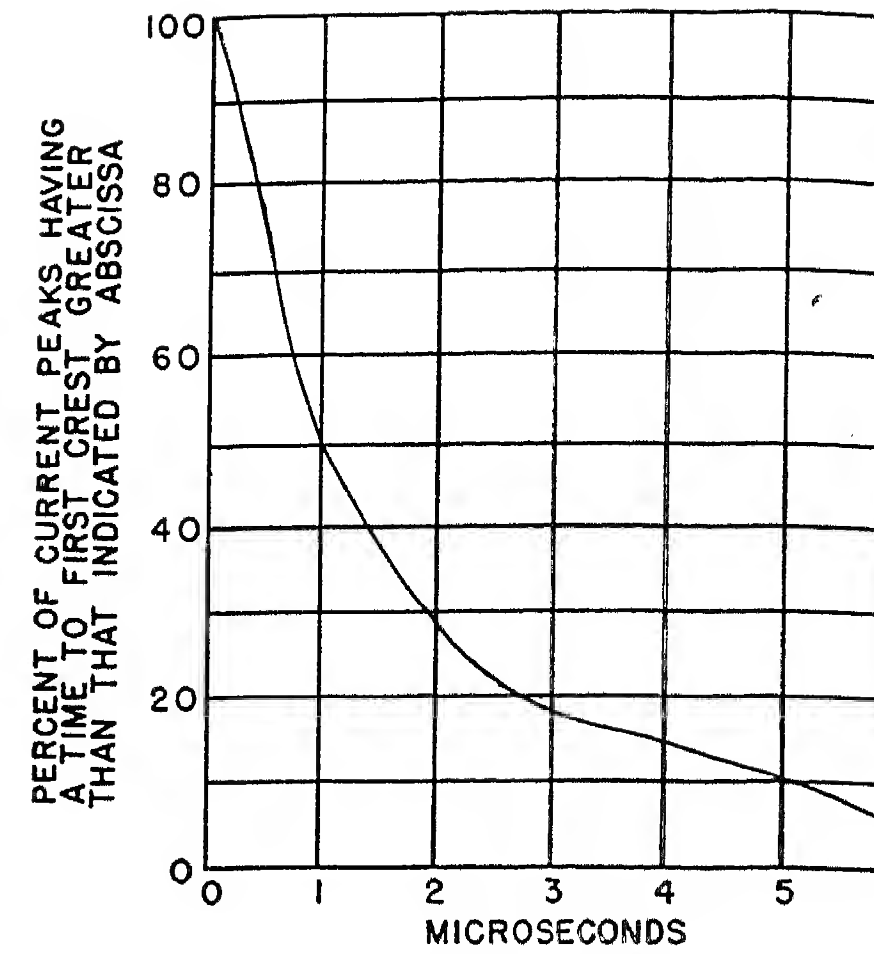


Figure 9. Time to first crest of current peaks as a function of frequency of occurrence. Based on 82 current peaks

a large number of valuable data concerning the lightning stroke. A few of the more important findings are:

1. Proof that current in the stroke as measured by oscillograph can be correlated with density of the stroke image on the photographic film,[5] thus definitely establishing the existence of continuing currents in the lightning stroke, similar to a d-c arc. Figure 13 gives an example of the close relation between stroke current and light emission for the continuous current components. The photograph shows longer persistence.

2. Discovery of the fact that lightning strokes are generally initiated by upward stepped leaders from the Empire State Building. Subsequent continuous downward leaders originate from the cloud.

3. The initial upward leader upon reaching the cloud is not followed by the so-

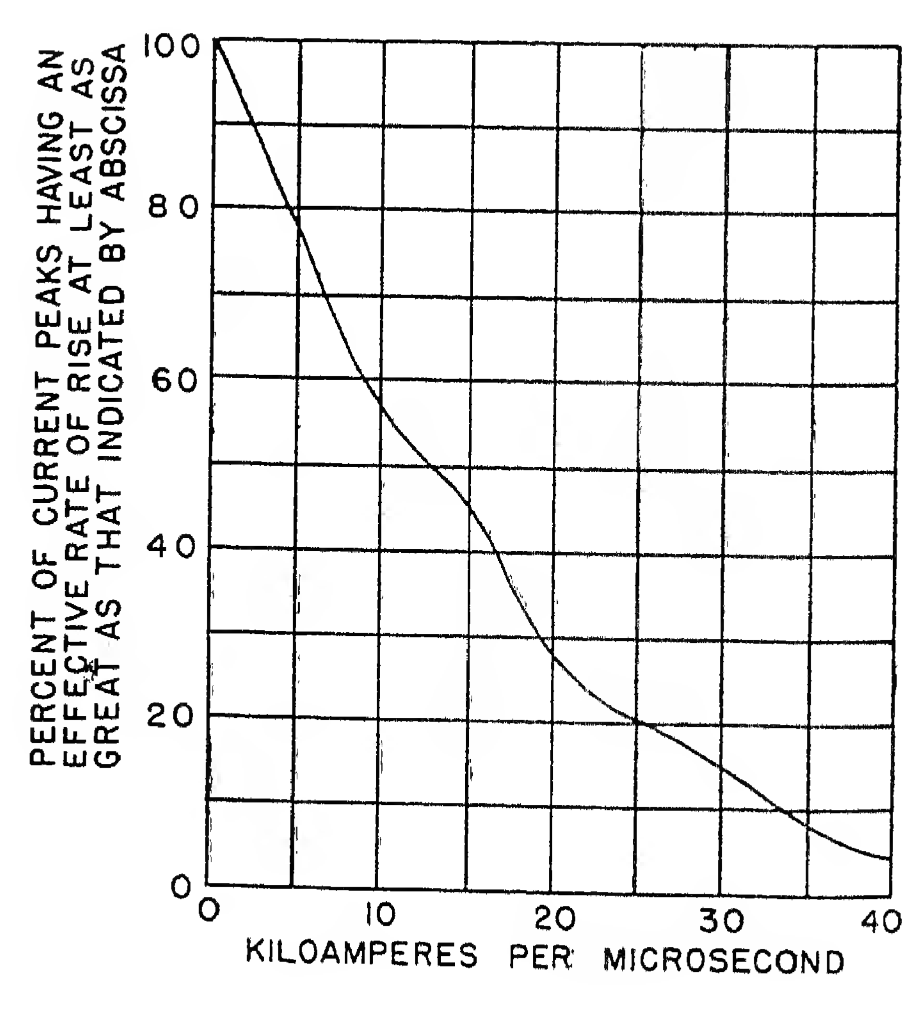


Figure 10. Effective rate of rise (slope of line through points on wave front at 10 per cent and 90 per cent of first crest) of current peaks as a function of frequency of occurrence. Based on 71 current peaks

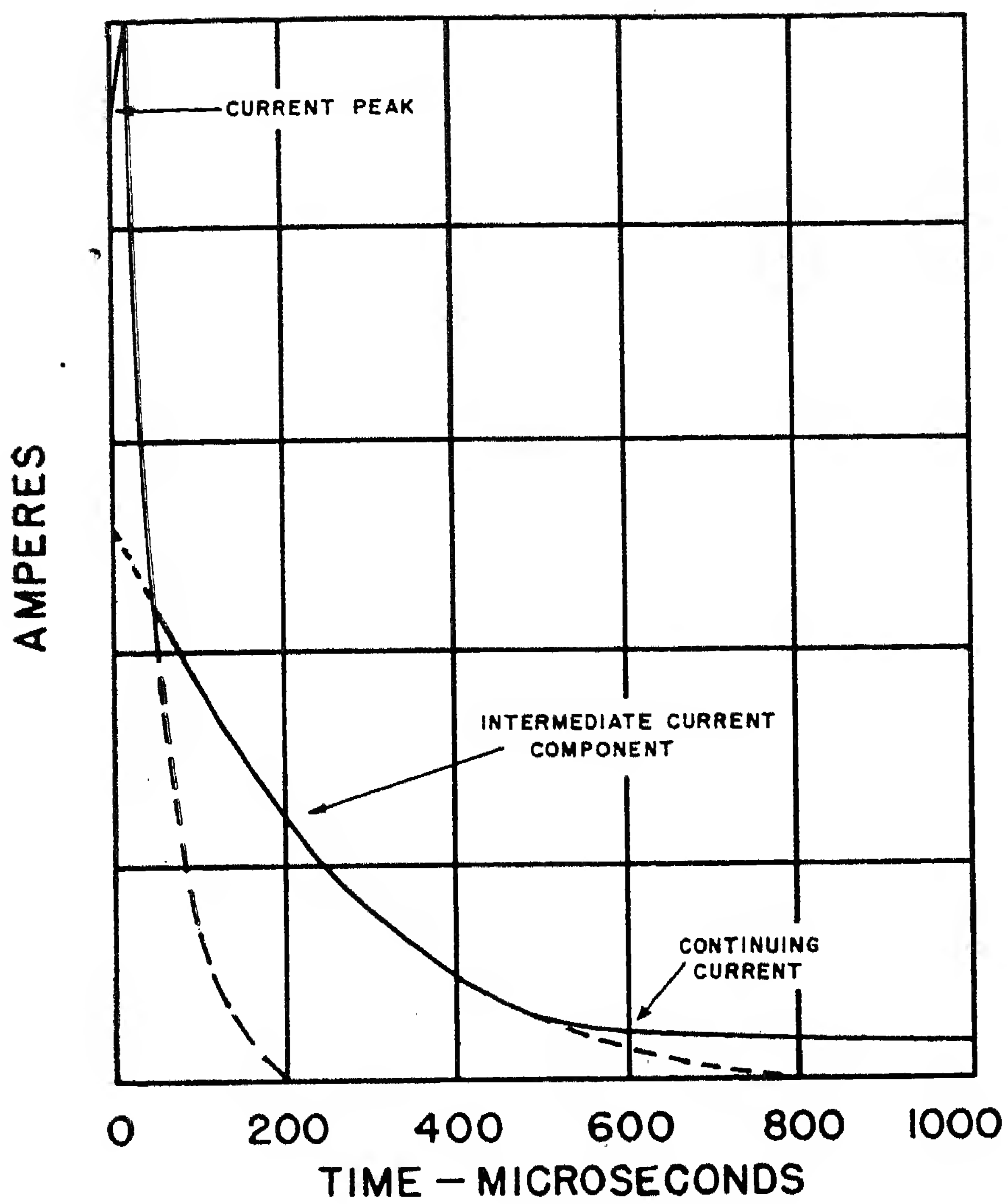
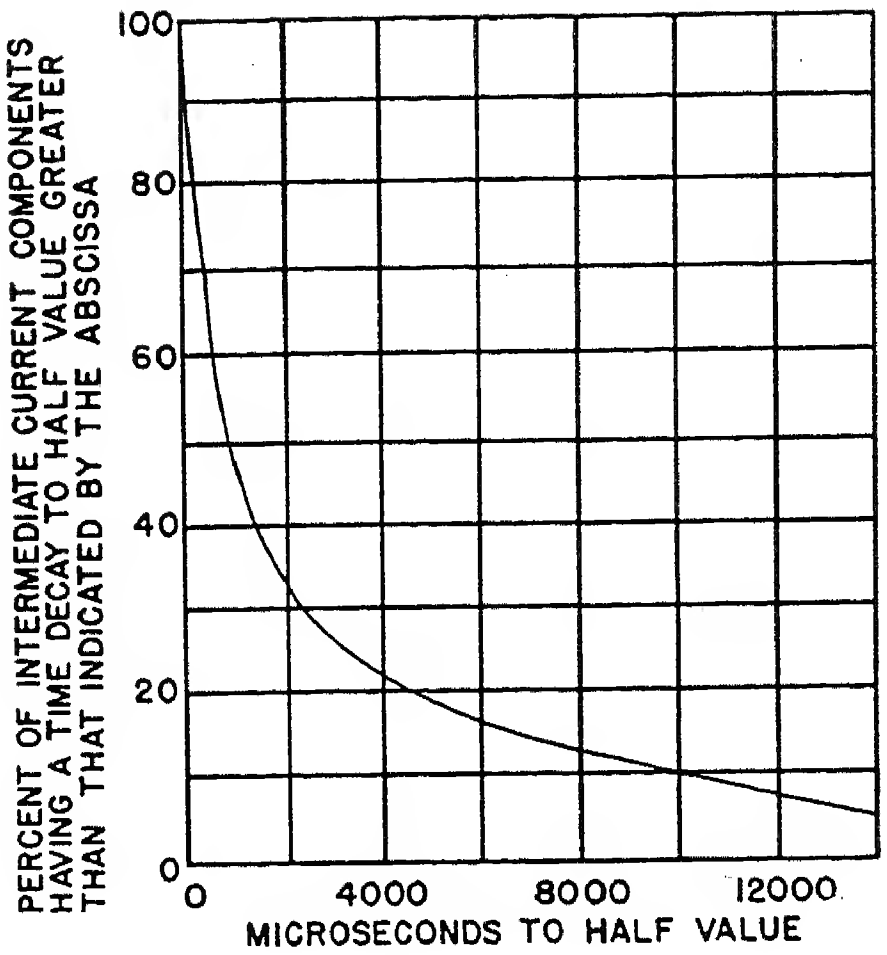


Figure 11 (left). Illustration of change from a current peak condition to continuing current by means of an intermediate current component

Figure 12 (right). Frequency of occurrence of duration of intermediate current components following high current peaks. Based on 29 peaks



called return stroke. This is attributed to lack of concentration and mobility of charges within the cloud as compared to the condition in the ground where charges generally are readily available when a downward leader makes contact with the earth.

4. The cloud is capable of sustaining long duration discharges of several hundred amperes for times of the order of 0.5 second and more.

5. In spite of the presence of a discharge channel, formed by continuous flow of current conducting charges of as much as 50 coulombs, the cloud can initiate a downward continuous leader, resulting in a return stroke upon reaching ground.

6. The velocities of propagation of upward stepped leaders fall within the range of the downward stepped leader. Continuous downward leader velocities are of the same order of magnitude as those to ordinary terrain.

7. Information on the front, tail, and crests of current peaks, as well as the charges in current peaks, has been obtained for approximately 80 peaks.

8. Considerable data are reported on the total duration (maximum 1.5 seconds) and total charge (maximum 164 coulombs) involved in lightning strokes.

Discussion of An Upward Leader

Figure 14 shows an upward leader from stroke number 17, 1947. The initial portion is not clearly visible. At a height of 300 feet above the building, regular steps with their bright tips can be distinguished easily. They occur at intervals of approximately 25 microseconds. At approximately 530 feet and later at 920 feet, there are two periods where the individual steps disappear and instead a continuous progress of the bright leader tip is indicated at an average velocity of the order of 1.5 feet per microsecond. In the later period, a slight light modulation indicates a slow increase and decrease of current flow. Between these two long periods, a number of leader tips also advance continuously although for a much shorter time. The same process occurs after the upper continuous portion has ceased. Close examination of the film after 1,100 microseconds indicates again slow light fluctuations of minor amplitude which would indicate that the progress of the tip is continuous.

The low-speed oscillogram of the same stroke is shown in Figure 13. It starts with a current of 200 amperes and with relatively slow oscillations, which persist for 11,000 microseconds, decreases to approximately 150 amperes. From that time on the current trace becomes very smooth and continues to drop. No rapid fluctuations corresponding to the original steps in the leader can be seen on the oscillograph film.

No explanation for this phenomenon is given, but it is quite certain that this con-

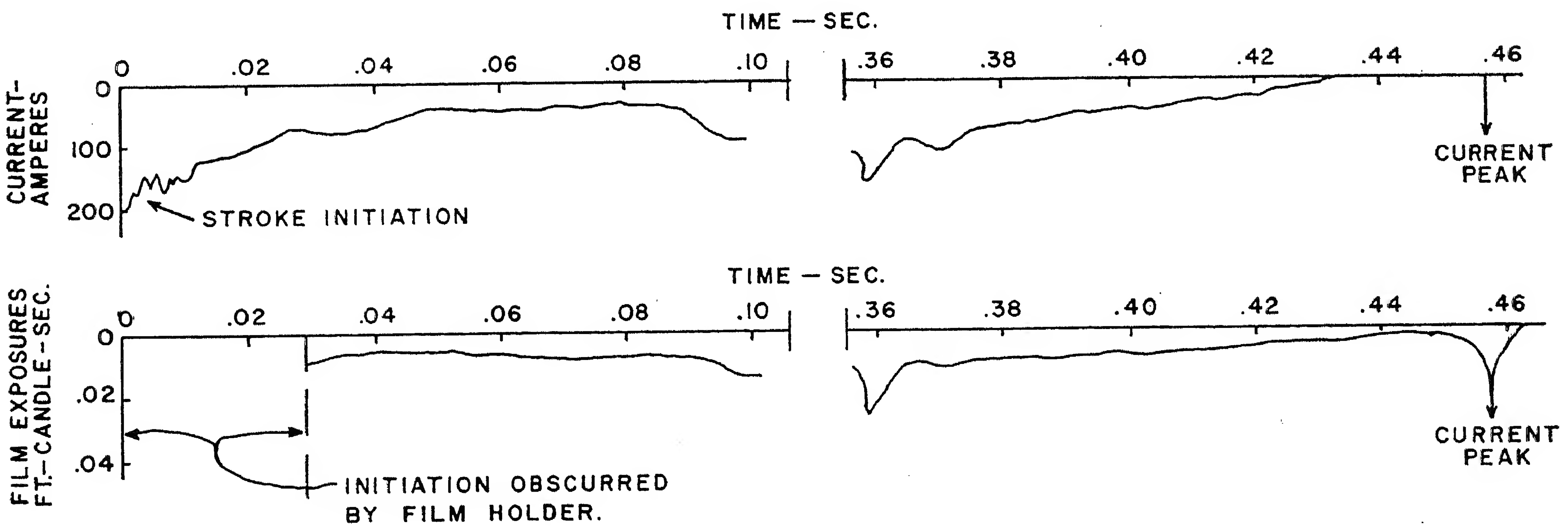


Figure 13. Comparison between continuing current measurements by cathode-ray oscillograph and the light emitted by the same stroke as obtained by camera. Stroke number 17, 1949

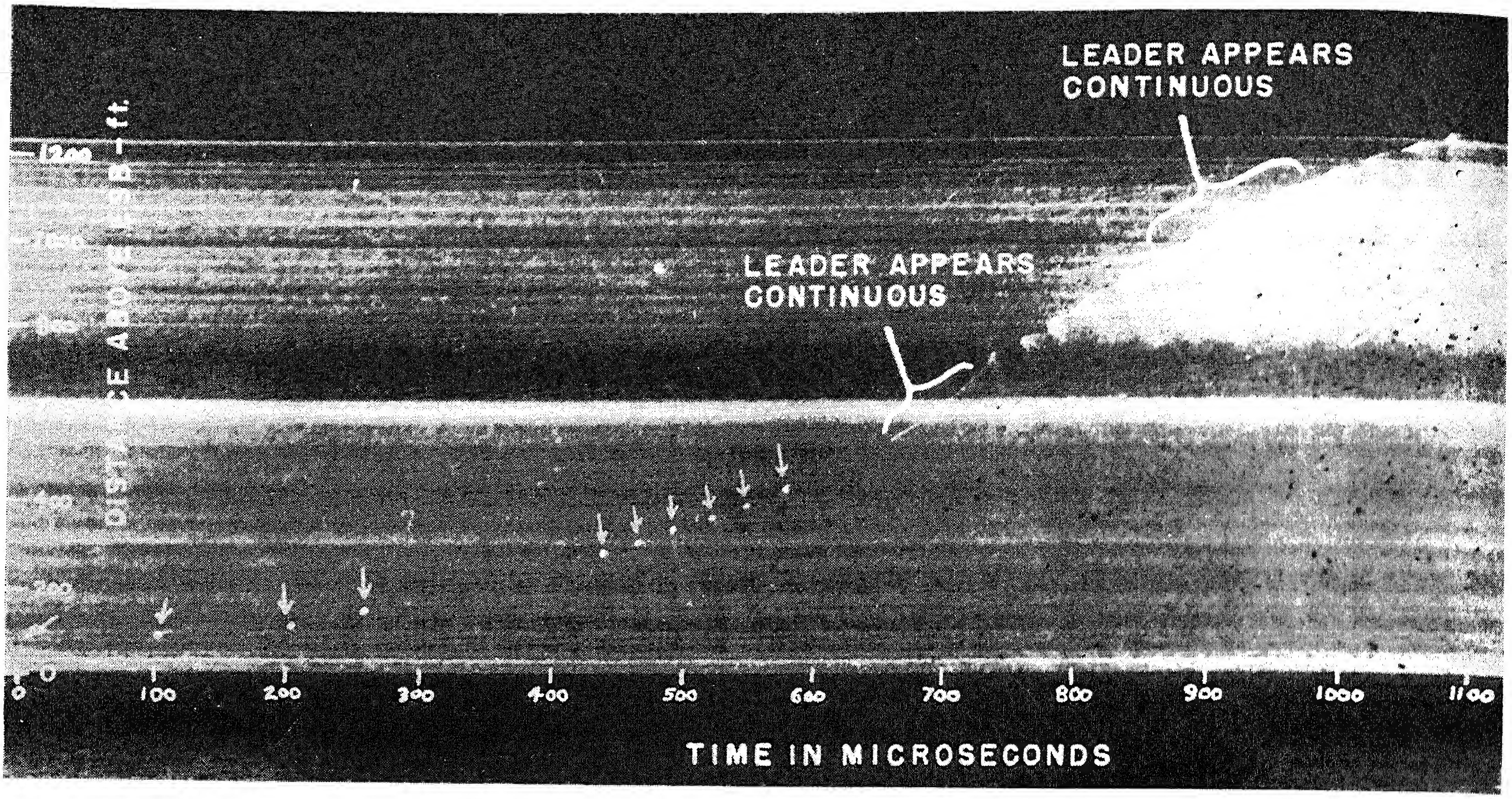


Figure 14. Upward stepped leader from Empire State Building showing continuous current portions. Stroke number 17, 1949

tinuous advance of the tip is not due to variation in direction of the stroke with respect to the camera. Both the oscillograms and the camera indicate that current flows continuously and, therefore, no deionization occurs in the channel for long periods of time during the upward leader process. The velocity propagation of the leader tip during the continuing portion is approximately twice (1.5 feet per microsecond) as high as during the original stepping process. This change in velocity is rather abrupt and occurs at 450 microseconds when the stepped leaders occur at intervals of 25 microseconds as compared to an original step interval of 81 microseconds.

Comparison With Other Data

Tables II to IV are included to show comparison between measurements of certain of the stroke and current peak characteristics obtained at the Empire State Building and elsewhere.

The largest discrepancy between results is in the total stroke characteristics—duration and charge. This may be due to the different stroke mechanism at the Empire State Building. Here quite a number of strokes result in continuous current discharge only, initiated by an upward step leader. Therefore, all the charges involved in the stroke would be measured while for strokes initiated by downward stepped leaders to ordinary terrain, the charges required for the stepped leader mechanism would not be measured at the ground end of the stroke. It is quite possible that the higher charges measured at the building are due to the fact that it is connected into a large network of pipes and thus can drain readily a larger area than would be possible in ordinary terrain. This same reasoning would apply to the stroke duration.

Shorter wave fronts were obtained at the Empire State Building than to other objects.[8] The wave fronts as well as the wave crest to a large extent are determined by the charges existing in the leader

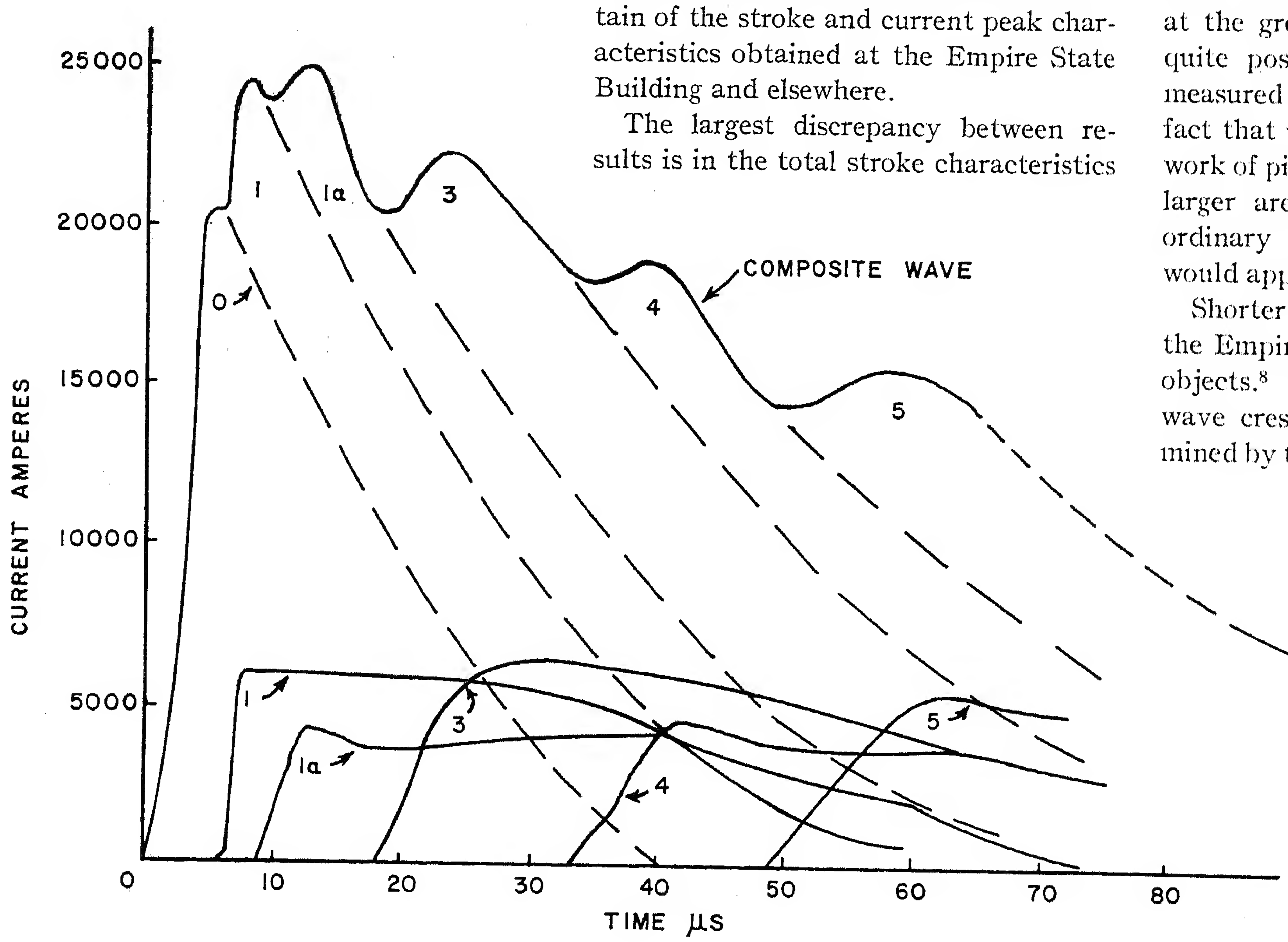


Figure 15. A current peak to the Empire State Building divided into current components supplied by charges from various zones, number 1 to number 6 of Manhattan Island

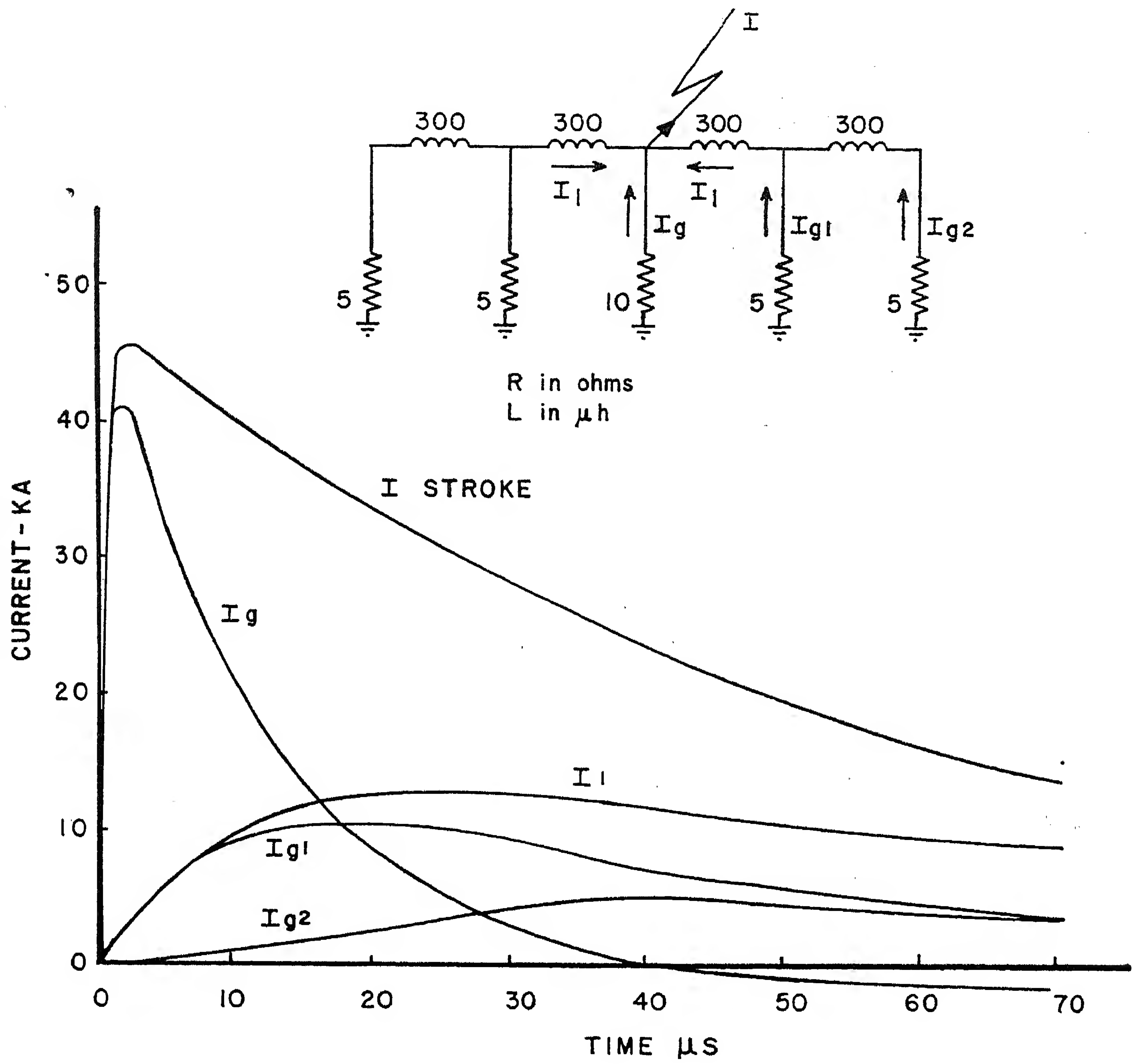


Figure 16. Current components in ground wires and towers for a lightning stroke to a transmission line tower

channel and by the charges available in the ground at the instant of contact. In the case of the Empire State Building, charges may be more readily available and, therefore, the current surge may reach crest more quickly than at other locations.

With regard to the tail, it has been suggested that the shorter tails at the building may result from shorter length of stroke path and consequently lower available charge in the leader channel. Such an explanation is very doubtful and in part is contradicted by the very much longer tails measured in a few cases, see Table IV. Such a reasoning assumes that the negative charge propagation from the cloud ceases once the leader has made contact. There is no reason to believe that this is true. On the contrary, the fact that the initial upward leader can drain considerable negative charges from the cloud would suggest that the cloud in the reverse process can and will continue to send charges toward ground after the downward leader makes contact and the return stroke progresses upward. This mechanism would not affect the front or the crest of the wave but would have considerable influence on the tail after a few microseconds. Therefore, the length of the stroke channel should not cause differences between wave tails at various locations. In the comparative study[8] the current peaks were measured with the high-speed Fulchronograph,[9] and it would appear that the accuracy for wave tails shorter than 50 microseconds would not be as great as that of the cathode-ray oscillograph.

Considering the difference in location, the great range of current wave shapes, and the different types of measuring instruments involved in the various investigations, the over-all check is amazingly good and permits the use of all of the data with greater confidence.

Table II. Crest Values of Current Peaks

Comparison of Results of Various Investigations

Per Cent of Cases Exceeding Value Given in Succeeding Columns	Direct Strokes, Kiloamperes: Empire State Bldg.	Direct Strokes, Kiloamperes: McCann[8]	Transmission Line Towers, Kiloamperes: Lewis[13] and Foust	Transmission Line Towers, Kiloamperes: Waldorf[14]
50	10	5.5	9.5	11
20	18	15	25	23
10	26	25	34	32
Maximum	58	160	132	114

Table III. Rate of Rise of Current Peaks

Comparison of Results of Various Investigations

Per Cent of Cases Exceeding Value Given in Succeeding Columns	Direct Strokes,* Kiloamperes per Microsecond: Empire State Bldg.	Direct Strokes,* Kiloamperes per Microsecond: McCann[8]	Transmission Line Towers,† Kiloamperes per Microsecond: Berger[15]
50	12.5	9	11
20	26	17	21.5
10	33	35	31
Maximum	50	45	

* Effective rate of rise (10–90 per cent).
† Maximum rate of rise.

Application to Transmission Lines

It is necessary to distinguish between a stroke to a line wire and a stroke to a ground wire or tower. In the first case the leader stroke current in the early stages can be neutralized only by any positive charges accumulated on the conductor within a few thousand feet of the stroke contact.

Since these charges are limited compared to charges available in the ground, the resulting stroke current must have a considerably lower amplitude than those given in Table II. If this current I is sufficiently high, the voltage which is the

Table IV. Lightning Stroke and Current Peak Characteristics, Direct Strokes

Comparison of Results of Various Investigations

Per Cent of Cases Exceeding Value Given in Succeeding Columns	Total Stroke Duration, Seconds: Empire State Bldg.	Total Stroke Duration, Seconds: McCann[8]	Total Stroke Charge, Coulombs: Empire State Bldg.	Total Stroke Charge, Coulombs: McCann	Front, Microseconds to Crest: Empire State Bldg.	Front, Microseconds to Crest: McCann	Tail, Microseconds to Half Value: Empire State Bldg.	Tail, Microseconds to Half Value: McCann
50	0.27	0.08	19	9	1	2.5	34	42
20	0.44	0.3	56	39	2.7	4.2	68	58
10	0.55	0.5	81	88	5.1	5.5	97	70
Maximum	1.5	1.3	164	100	8.3*	10.0	120	90

* Maximum normally encountered—two unusual peaks exceeded this value.

product of $I(Z/2)$ will attain values causing insulator flashover. The currents required are of the order of 1,600 to 5,000 amperes for transmission lines with highest system voltages. Once sparkover occurs at the nearest tower, the stroke can then increase to the values indicated in Table II because the total accumulated ground charges are available. This process is not entirely clear and currents involved cannot be readily deduced from direct stroke data.

There is another complication to be considered. Before the last step of the step leader makes contact with the wire, the potential difference between its tip and the wire must be sufficient to cause breakdown of the remaining space. When this occurs to ground, this potential largely collapses because of the large available positive charge. In the case of the wire with limited charge, a considerable amount of potential may be applied to the wire without great increase in current. This process would explain the destruction and sparkover of several adjacent poles on wood pole lines which otherwise would require currents in excess of 10,000 amperes.

If the stroke contacts the tower or the ground wire, then the crest values and wave shapes indicated in Tables II to IV are directly applicable. As has been pointed out before, it is quite likely that the ground conditions of the stroke may have a considerable influence on the phenomena taking place at the point of stroke contact, the wave shapes depending largely on the availability of charges; nevertheless, the conditions at average transmission lines with ground wires would not be much different from the Empire State Building as far as current peak wave shapes are concerned. However, maximum current amplitudes on transmission lines have reached considerably higher values than at the Empire State Building.

The principal difference in the two cases would occur during the front portion due to the charge available in the building. Directly after this the building in effect becomes part of the stroke channel collecting charges from the surrounding areas. The differences may not be great because certainly there does not seem to be any lack of charge in some strokes to transmission lines. In such cases, therefore, the charges must be collected in the immediate vicinity of the tower in order to produce such high amplitudes. Analysis of magnetic link records on transmission lines indicates that stroke current fronts must be relatively steep in order to account for the measured distribution of currents in ground wires and towers remote from the stroke.

In the case of the Empire State Building, as well as on the transmission line with ground wires, the tail of the wave is greatly influenced by charges arriving from relatively long distances. Figure 15 shows a current peak to the Empire State Building.[10] The oscillation on this current wave can be explained by calculating the arrival of charges or current waves from different points of the shore line of Manhattan. The minimum points of these oscillations present the arrival of these current waves which then are superimposed on the tail end of the currents resulting from the superposition of the previously available current flow from sources closer to the building.

The time to the minima point has been analyzed for a period of years and was found to be very regular. These minima, therefore, must be due to fixed ground conditions and cannot be explained from changes in the lightning channel, which cannot be expected to be as regular. All the minima points are not always present. This indicates that at the time of such strokes, charges are not available at some locations, perhaps having been drained by a lightning discharge elsewhere.

In the case of the Empire State Building, it is not possible to calculate the individual current components because of the unknown characteristics of pipe and cable systems which eventually make contact in the waters surrounding Manhattan Island.

However, a close correlation exists in the case of transmission lines,[11] see Figure 16, where the current distribution can be calculated for a given stroke current, either by repeated reflections or more simply by equivalent circuits.

The similarity between Figures 15 and 16 is striking. The current peak and front largely depend on the charges available close to the point of contact. The wave tail portion supplied from the region immediately close to the point of contact is relatively small in both cases (about 30 per cent in the stroke to the Empire State Building). The bulk of the charges is carried to the stricken object from a considerable distance. In the case of the Empire State Building indications are that charges were drawn from as far away as 10 miles. Transmission line studies also indicate currents as high as 5,000 amperes flowing in the ground wire five to ten spans distant from the stricken tower, although tower currents in these sections may be less than 1,000 amperes.

Calculations[12] were made for four strokes to determine the stroke wave shape from magnetic link data of currents in towers and ground wires. The crest of these calculated current waves ranged between 57,000 and 100,000 amperes, the wave shapes between 7x25 and 2.5x70, the rate of rise between 13,000 and 31,000 kiloamperes per microsecond. These values are well within the range of the data presented for current peaks at the Empire State Building.

From such comparisons, one arrives at the conclusion that the use of the wave characteristics presented in the paper is well justified for calculations of lightning stroke performance on transmission lines.

As previously explained,[2] the effect of the continuous current components may cause burning of the wire at the point of contact, may interfere with the operation of automatic reclosing equipment, and in isolated neutral systems may place high thermal stresses on arresters. In distribution circuits this component may cause fuse blowing but due to the multiplicity of paths to ground, lightning arresters should not be affected by such discharges.

Conclusion

The information obtained during two separate periods of investigation gave results which checked each other very closely. Comparison with data by other investigators indicates the validity of the data for application to direct stroke problems on transmission lines. The stroke characteristics as they apply to the ground end of the stroke now are well classified. Numerous problems remain to be solved concerning the exact physical processes involved in the formative state of the lightning stroke, the interaction between leaders and ground charges, the distribution of current along the stroke channel during the return stroke, and cloud to cloud discharges.

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Discussion

H. L. Rorden (Bonneville Power Administration, Portland, Oreg.): The data presented by the authors in their long-continued study of lightning phenomena indicate the complexity of the nature of lightning and the necessity for continued study in order that lightning phenomena may be understood thoroughly. This in turn must ultimately lead to a better understanding of method protecting transmission systems from lightning outages and damage. The efforts of these authors over a period of years has contributed a vast amount of information that is of indispensible value to the industry.

The Bonneville Power Administration has over 2,400 miles of 230-kv transmission lines that are not protected with overhead ground wires except for 1 mile adjacent to the substations. While most of the lines are located in an area of low isokeraunic levels, around the Grand Coulee Dam, definite areas exist in which the transmission lines average 3 to 4 outages per 100 mile-year.

The primary method of prevention of sustained outages due to lightning hits has become high-speed relaying and high-speed automatic reclosing of 230-kv circuit breakers. The successful reclosing of the circuit breaker depends to a large extent upon the deionization time of the arc, and experience and experiments indicate that about 0.25 second dead time seems to be a practical minimum at the present time for 230-kv lines. Thus the authors' data indicating the existence of lightning currents of the magnitude and duration that may affect the time of arc deionization become of great importance. The authors' ability to measure and classify the data obtained leads to the conjecture as to how some of the data may be correlated with lightning strokes that occur on the transmission systems and with possible factory tests designed to show the equipment's ability to withstand surges to which they are subjected in service as found by the authors of the paper.

J. H. Hagenguth and **J. G. Anderson**: Mr. H. L. Rorden poses several questions beyond the scope of the present paper. The paper gives statistical data on direct-stroke currents and some application of such data to grounded structures, such as ground-wire systems.

The high-speed reclosing circuit breaker has considerable merit. The use of high-speed reclosing without ground-wire protection undoubtedly has merit in regions where the lightning incidence is low. In regions with higher isokeraunic levels, a ground-wire system might be found economical by preventing a large number of temporary outages due to currents of low amplitude.

Mr. Rorden does not state whether the practical minimum dead time of 0.25 second is due to continuous lightning currents or to other system characteristics. It would be interesting to have oscillographic data which would disclose the reasons for this rather long dead time.

We believe that the lightning stroke current data now available can be used in many cases to calculate system behavior within engineering accuracy. The data indicate that present standard impulse waves are a very good representation of surge waves to which apparatus is exposed in the field.

Sleet-Thawing Practices of the New England Electric System

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Synopsis: Sleet loading on transmission lines became a serious problem during the early growth of the New England Electric System. As a result, a method was devised to prevent ice from accumulating on conductors of the then existing lines, and basic changes were made in the design of projected lines for greater reliability during icing conditions.

This paper describes the types of sleet-thawing operations developed for the protection of lines vulnerable to damage and also power requirements, operating features, and safety precautions to both personnel and power-supply equipment.

THE first serious sleet trouble on lines of the New England Electric System occurred in 1916. During November 1920, a sleet storm caused recurrent wire breakage in a 4-mile section of a 69,000-volt line running through the Berkshire Hills. This storm lasted for 5 days. A last desperate attempt was made to prevent further breakage after temporary repairs by using a heating current to clear the line of ice. While the success of this experiment was limited, it did provide the incentive for further studies and tests, so that in the following year lines equipped with thawing connections survived a storm which did widespread damage in western and central Massachusetts.

A snow and wind storm in 1924 caused extensive damage to lines in southern areas, after which thawing precautions were extended to all high-voltage transmission lines of the system which are vulnerable to damage. In the following years, frequent use of sleet-thaw facilities has been applied, with the result that no repetition of the early disasters has occurred.

Following the widespread damage caused by sleet in 1921, system engineers made radical changes in the design of transmission lines for greater reliability during icing conditions. These improvements involved conductor configuration and greater structural strength of towers.

The design of the more modern lines of

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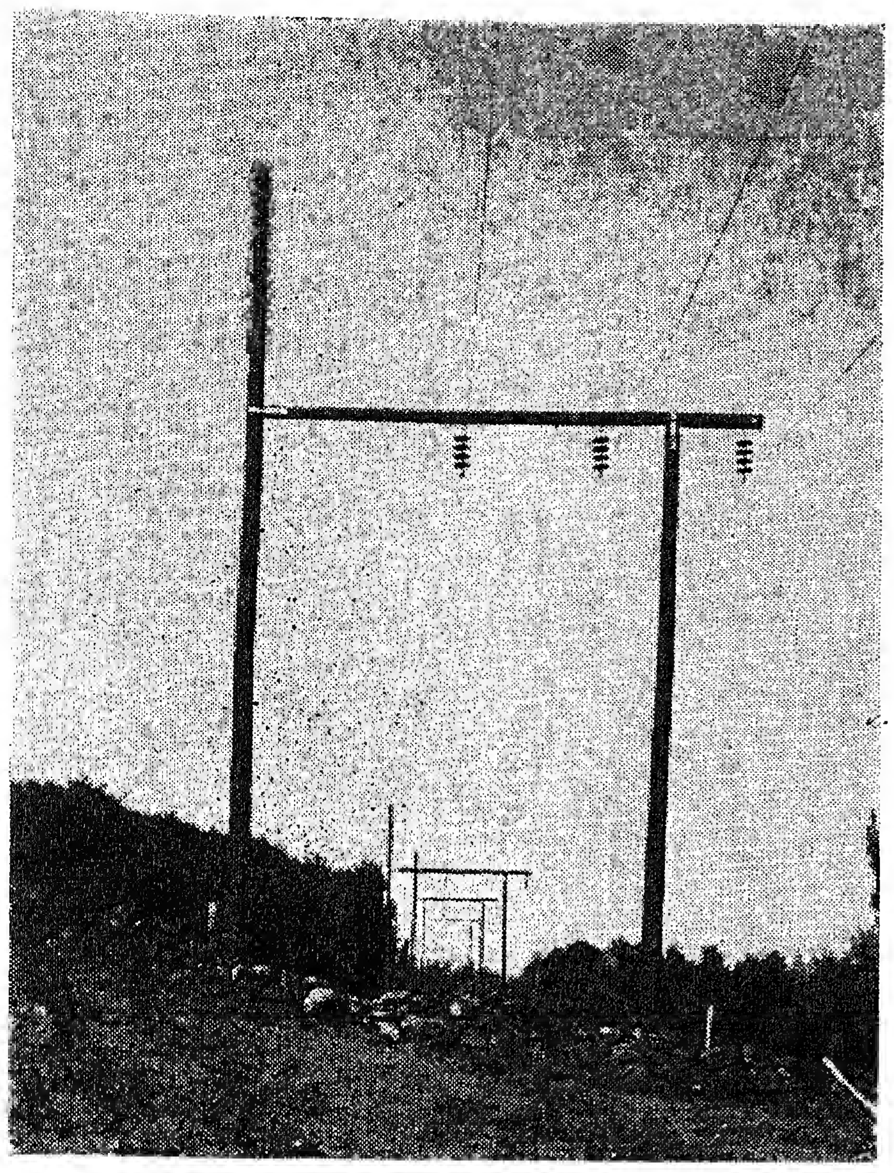


Figure 1. Modern 69,000-volt single-circuit suspension-type line with horizontal pole crossarm and high vertical pole for the ground wire support. This construction is designed to withstand all expected ice and wind loading conditions

the system incorporates features and use of materials which provide an optimum economic protection against destruction by sleet. These are:

1. Horizontal configuration of power conductors to avoid jump contacts when ice unloads from lines.

2. Ground wires offset from the vertical over power conductors.

3. Lower working tensions for conductors and ground wires.

4. Flexibility in structures by the use of wood poles in both vertical and horizontal members.

A line having all of these features is illustrated in Figure 1. No thawing arrangements are normally provided for such lines. The steel tower lines having most of these features have shown good performance during sleet storms experienced to date without thawing precautions. However, information is available so that a last stand emergency thaw can be made on 260 miles of such lines in a series connection should the occasion arise.

Extent of Thawing Precaution

All presently arranged thaws are for steel tower lines of the older designs and for the newer pole lines of light construction serving the more important suburban loads. These include 450 circuit miles of 69,000-volt lines and about 50 circuit miles of lower voltage circuits. The extent of this sleet-thawing activity is shown geographically in Figure 3.

The older tower lines, in general, are of pin-type construction or have vertical power conductor configuration. The lines having vertical configuration are the most vulnerable to service outages and structural failures by reason of the jump contacts which may occur when ice drops from the wires. The resultant burning reduces the conductor strength sufficiently to cause failures under the added ice loads, and the consequential unbalanced loading on the towers causes their structural failure. This is displayed in Figure 2, wherein a tower line failed on a right-of-way alongside a pole line which remained undamaged by ice loading.

The extent of thawing activities requires participation by over 40 men specifically assigned to these duties at 27 scattered locations.

Thawing Policy

The primary purpose of thawing precautions is to prevent an accumulation of ice on the wires rather than to melt after a build-up. This is not entirely feasible, due to the requirements for continuing service. However, the policy is to thaw too soon rather than too late, so that the accumulation on any particular line is that built up only during the interval of a schedule when the conductors are not carrying a heating current.

Any indication of sticking is a sufficient reason to thaw. This is the general policy of the New England Electric System. Thawing is considered as insurance for which a premium must of necessity be paid, that premium being the cost of labor and power for thawing and the temporary loss to service requirements of the lines and equipment involved. Guessing the weather by individuals or personal appraisals of storm situations are considered inferior to making use of this insurance.

In the last 10-year period, the system employed thawing schedules in 202 instances. In review, some were undoubtedly unnecessary, but the success in preventing damage justifies the action. Of these thawing operations, 70 were on lines in terrain of high elevation and the remainder were on lines at lower elevations where sleet conditions were prevalent in wide areas.

Power and Equipment Requirements

In general, a heating current is used to provide about 11 watts loss per foot of conductor, or 175 kw per circuit mile of line. A current of about 350 amperes is required for a 2/0 copper conductor when the ambient temperature is near the freezing point. Experience indicates that this is sufficient to clear a line of ice up to 1/2 inch of radial thickness within 30 minutes. This requires a voltage on the lines thawed of 500 to 600 volts per mile at 60 cycles and of 300 to 400 volts per mile where 25-cycle thawing sources are available. When all present thawing schedules are employed, this presents a demand for 27,000 kw and an equipment reservation of 100,000 kva, consisting of 80,000 kva in rotating machines and 20,000 kva of substation transformers. Reference is made to the voltage requirements for a

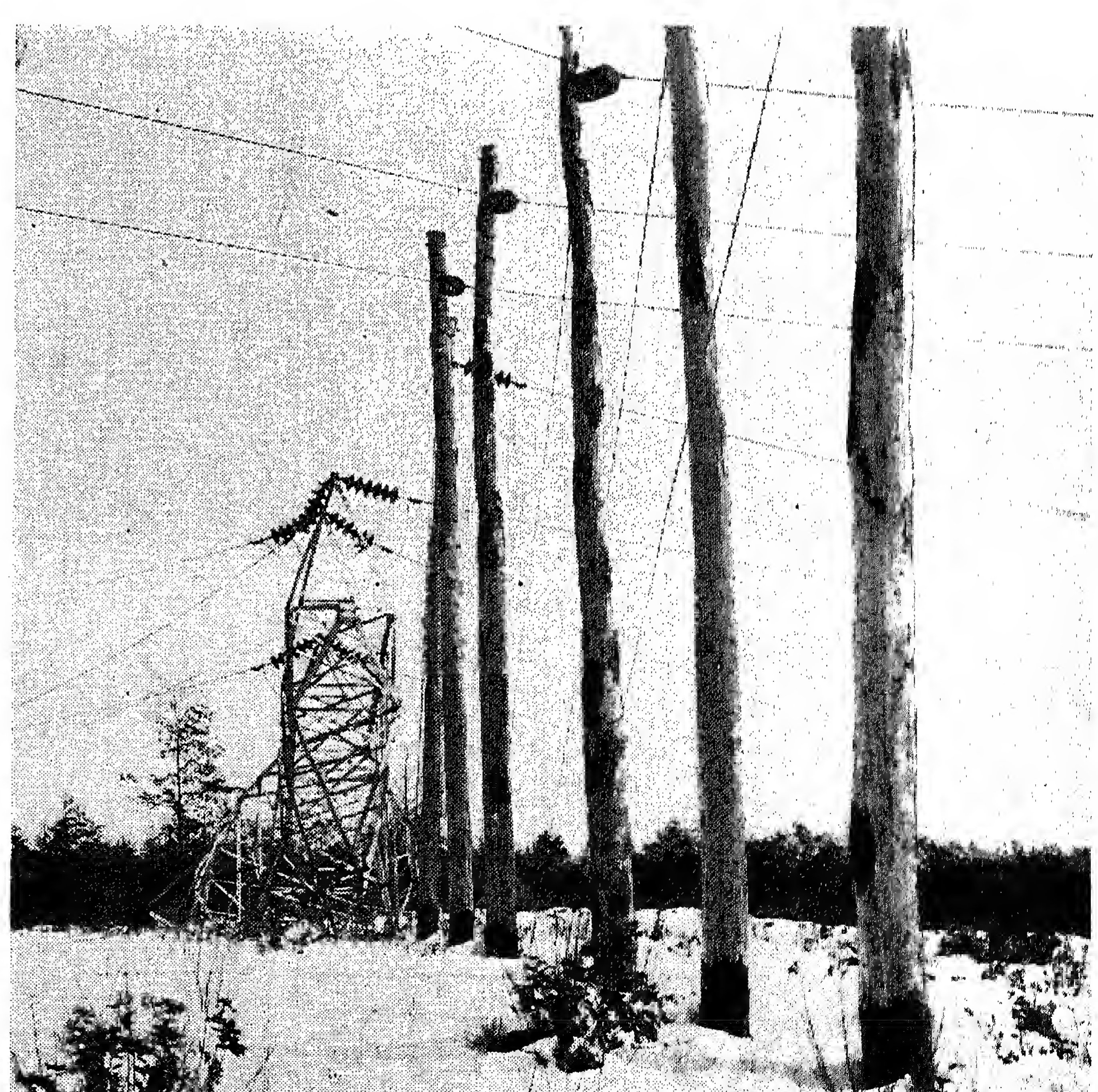


Figure 2. Failure of tower on same right-of-way with wood pole line which remained undamaged

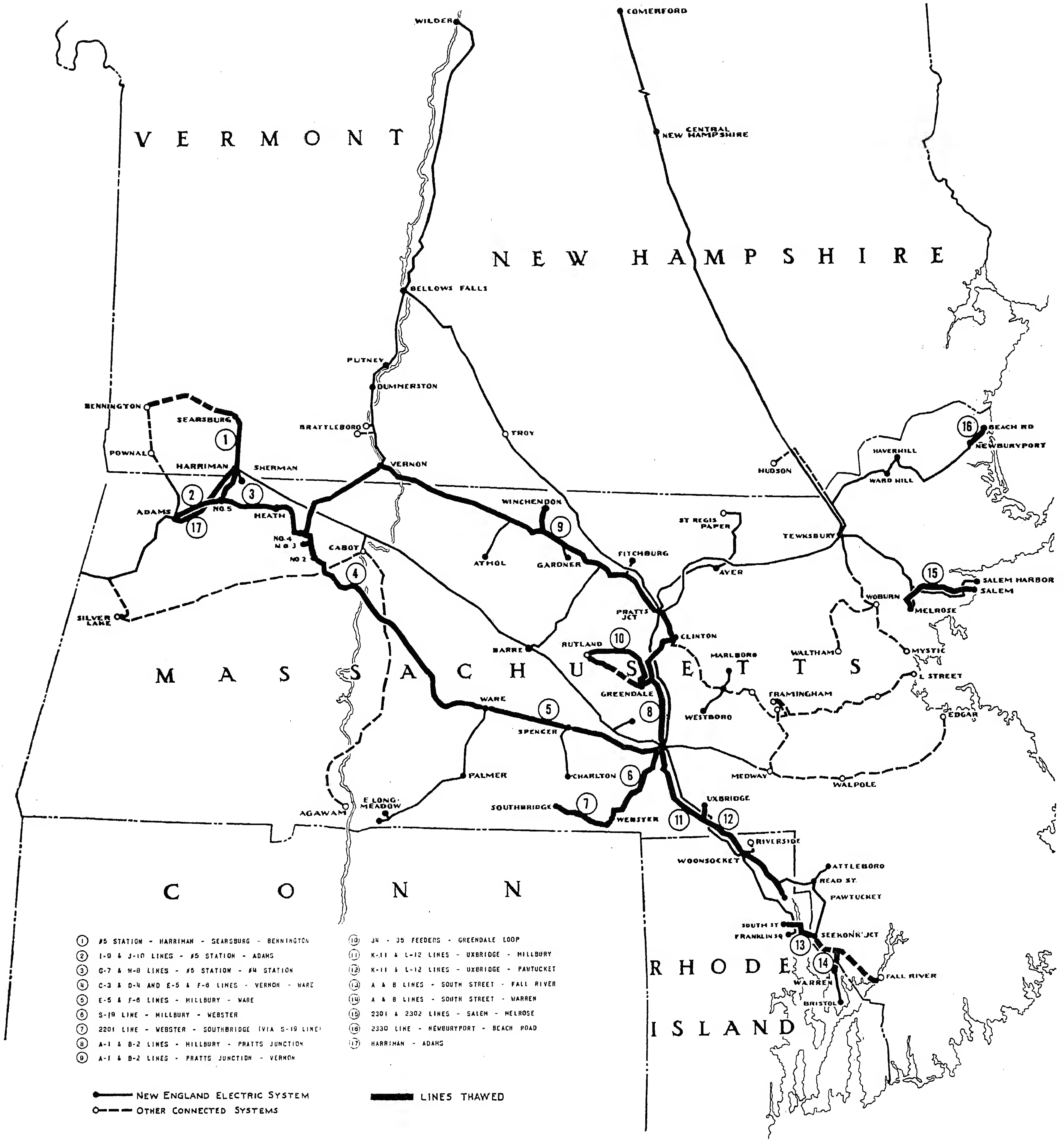


Figure 3. Geographic location of lines thawed

25-cycle heating current. This frequency is preferred for thawing rather than normal frequency, due to the lower reactance and, hence, lower voltampere requirements. To provide thawing for 97 circuit miles of 69-kv lines, 25-cycle equipment is available.

Sleet Observation and Detection

During periods of unstable weather conditions with snow or rain in the temperature band from 25 to 35 degrees Fahrenheit, hourly reports are received from over fifty observation points, which are recorded on a special dispatcher's sleet condition log at dispatching headquarters. These reports include the temperature, the weather, and information concerning the "stick" (or glazing) and whether the tendency is to build up or wash off.

At the higher elevations, particularly in the Berkshire Hills (elevation 2,200 feet), local residents are employed to act as sleet observers. Frequent contact with the United States Weather Bureau has proven helpful in anticipating conditions which might result in sleet formation. This allows time for planning load allocation for the release of lines which may have to be scheduled for thawing.

A sensitive and reliable sleet detector, as introduced by Langdon and Marquis,[2]

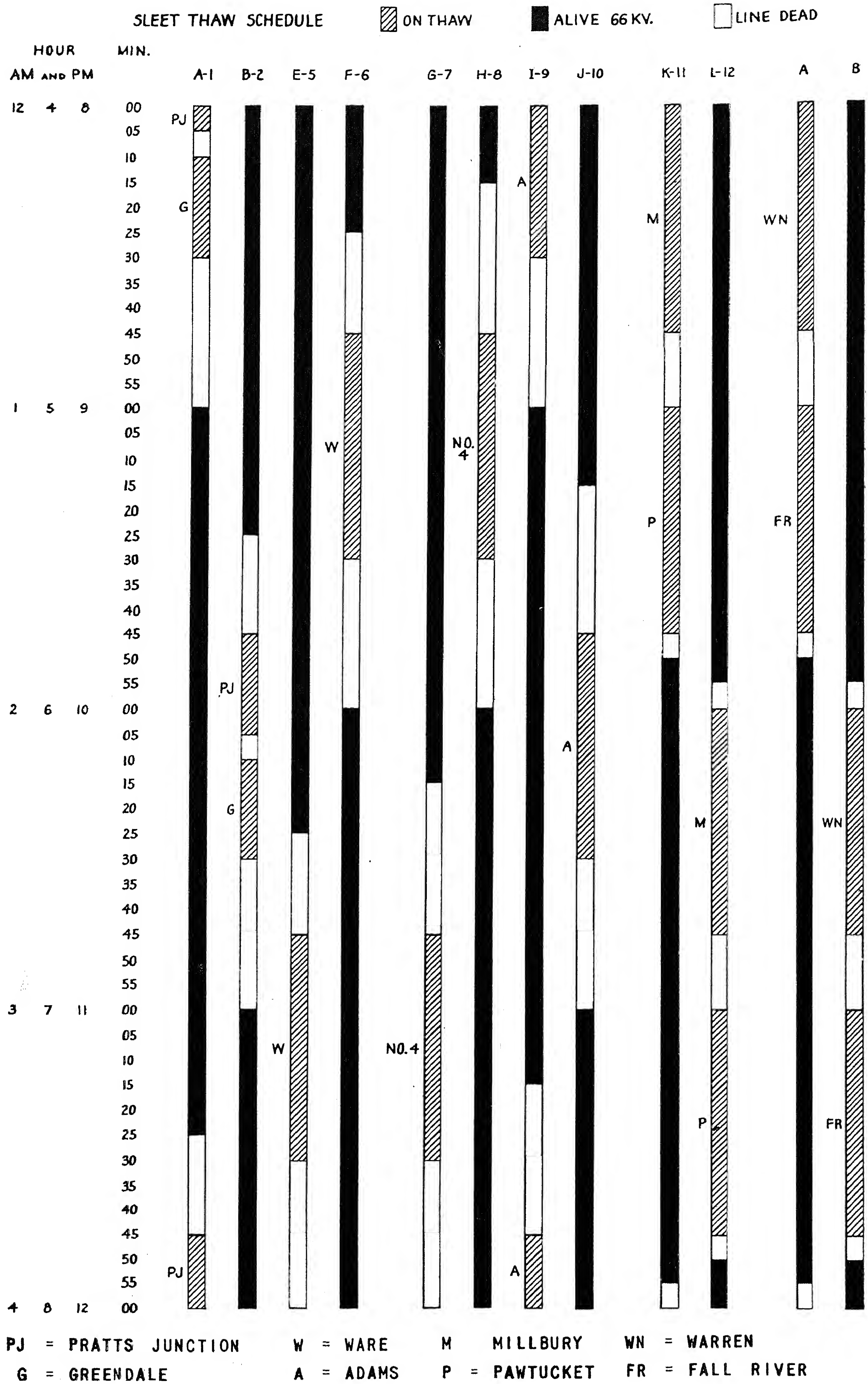


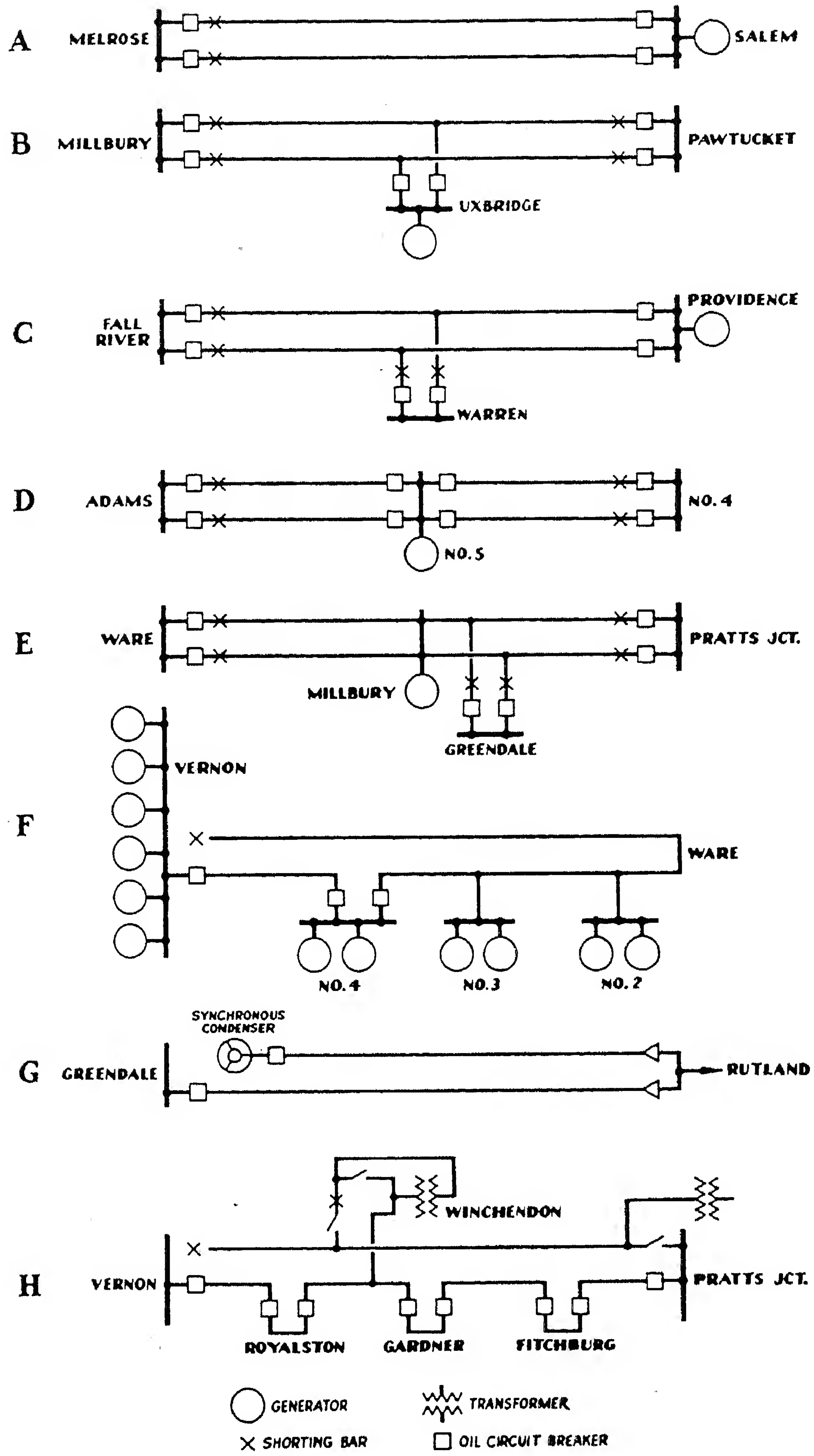
Figure 4. Graphical time representation of scheduled thaws

is used on 115-kv lines equipped with carrier current relaying. This detection is made by comparing the carrier's receiver signal strength during sleet-forming weather with a signal strength received when the wires are known to be clear of sleet. Due to skin effect, an accumulation of ice on the wires causes comparatively high attenuation of the carrier signal. The amount of attenuation is indicative of the thickness of ice on the lines. Some interpretation of this indication is required, based on a general knowledge of weather conditions in the territory. A judgment is made thereby as to whether a light accumulation is occurring over a long length of the line or a heavy accumulation in local areas.

Detection of attentuation is accomplished by inserting a resistor into the control grid circuit of the carrier receiver tube so that with a normal received signal, it operates at the top of its straight line portion rather than on the saturated portion of its characteristic. Reduction in signal voltage caused by sleet is then indicated on a milliammeter measuring the output current of the receiver tube.

Sleet detection by this method is now available on 287 circuit miles of 115-kv lines. While the 115-kv lines are not usually susceptible to damage by sleet, this indication is useful to determine specific areas in which sleet is forming. Suitable action can then be taken to protect any vulnerable lower voltage lines in the affected areas.

Figure 5. Diagrams of sleet-thawing connections



Types of Thaws

Three types of thaws are employed: scheduled, fixed, and supervised.

Scheduled Thaws

The first scheduled sleet thawing on the New England Electric System, and perhaps the first in New England, was conducted in 1921. It employed the usual means, now so commonly used, of attaching a short circuit to one end of a dead line and connecting a generator to the other. By increasing the generator voltage to a proper value, a current sufficiently high to melt sleet was caused to flow over the line through the short circuit.

Of particular interest in this pioneer thaw was the method then devised to thaw by schedule. All switching of lines out of service was done on a time basis as well as the attachment of the short circuit, the connection of low-voltage generators to terminal equipment, the build-up of thawing current, the back down, the generator disconnection, the removal of short circuits, the energizing of the line to normal high tension service, and the preparation of the next line to be thawed.

These operations were done by reference to a time schedule, and no communication between stations was relied upon nor used in the procedure.

The thought at that time was expressed by C. R. Oliver: "The success of the sleet-thawing arrangement depends upon the complete co-operation between the generating station and the station at the end of the line to be thawed. This co-operation normally requires telephone connection between the stations, but during sleet trouble the telephone lines are usually the first ones to be put out of commission, so it was necessary to devise a means of working the sleet-thawing device absolutely independent of telephone. Accordingly, a definite schedule was worked out for 24 hours whereby sleet could be thawed on any or all lines.

"This schedule is independent of dispatcher's orders or telephone, and it functions even when all telephone connections have been completely wiped out."[1]

Since 1921, the New England Electric System has extended the thawing schedules to include hundreds of miles of line with various combinations of tap line connections. The thought expressed in 1921 is still an integral part of all scheduled thaws, that is, that they shall function without further communication.

After the thaw has been started, there is no communication between plants with reference to this particular procedure. The thawing schedules include methods of procedure in the event that line failures occur during the thawing period, so that

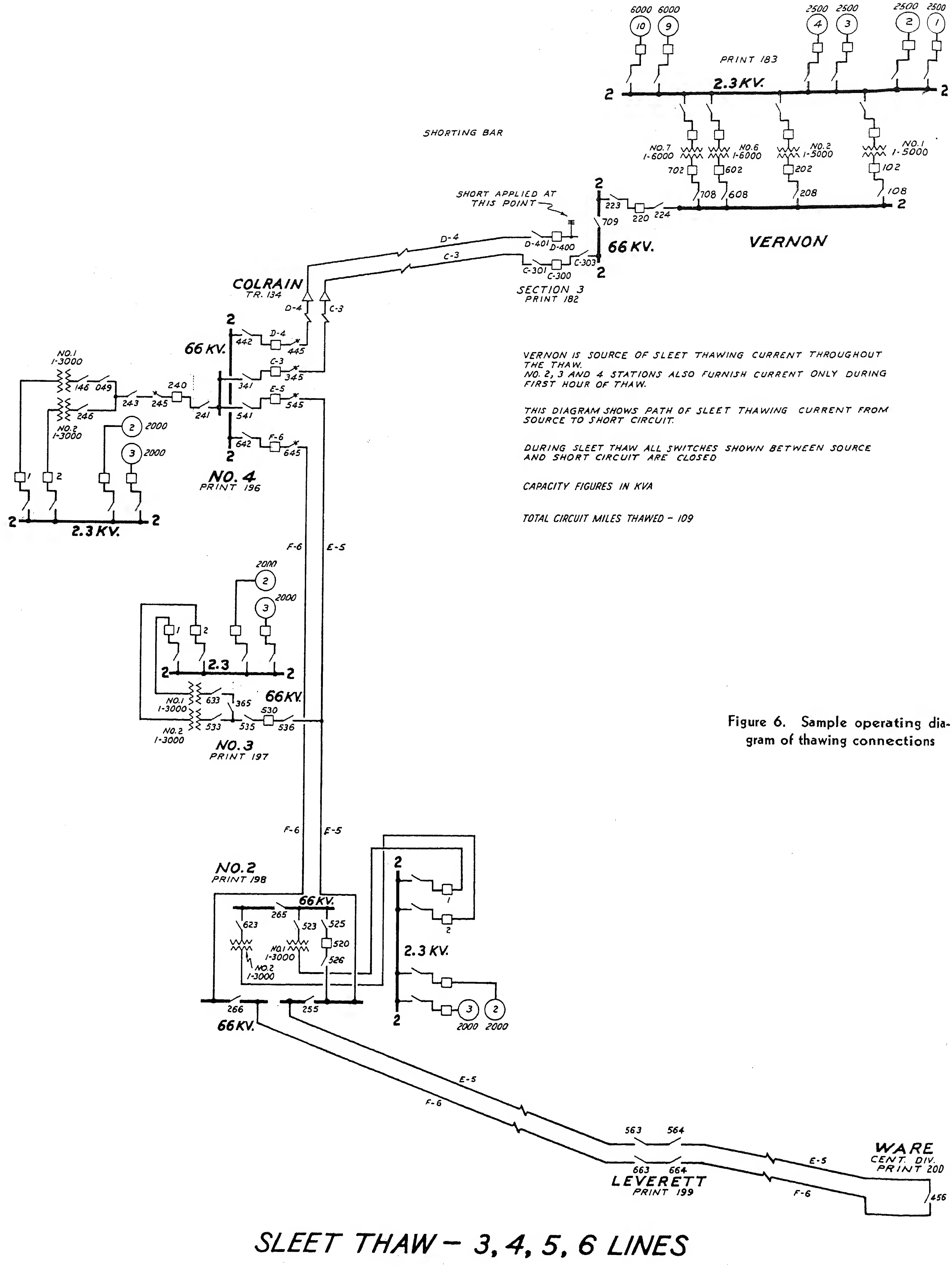


Figure 6. Sample operating diagram of thawing connections

service may be restored to stations served by one or the other of two lines. This is accomplished by the indication of loss of voltage on any line, no current, or a premature back down of thawing current. These are the indications that prompt the required procedure by the crews at the stations involved.

Figure 4 is a graphical time representation of the scheduled thaws. This diagram is a ready reference used by dispatchers to keep informed as to the status of lines (*A-1*, *B-2*, and so forth) in or out of normal service during the thawing periods. Operators at the various stations have written instructions to cover the timing and procedure of every switching move conforming to this graphical time schedule.

A 4-hour cycle is used on all scheduled thaws, which, in general, gives each of two lines 45 minutes of heat during the cycle.

Examples of scheduled thaws, including system connections, are given in diagrams, *A*, *B*, *C*, *D*, and *E* of Figure 5.

Fixed Thaws

This type is used where schedule thawing cannot be adapted to the system needs. In these cases, once the thaw is started, it remains for the duration of the storm, unless interrupted by a line failure. Communication facilities are used only to start and terminate the thawing operations. Operating rules are established to restore service without intercommunication as speedily as possible to any station without service as a consequence of a line failure.

Diagrams *F*, *G*, and *H* of Figure 5 are examples of the fixed type of thaw.

Diagram *G* shows a fixed thaw, whereby two feeders are tied together on the far end with one feeder opened at the station end. A synchronous condenser is operated at the opened station end either overexcited or underexcited, depending on load and voltage conditions. Heating of conductors in this 20-mile series connection is effected by the condenser current adding to the load current on these feeders.

Supervised Thaws

These thaws are switched under dispatcher's supervision completely and include not only the usual short-circuit setup but adjustment of system reactive and load to effect increases in current over lines and feeders in dangerous areas. Complete communications are required for these supervised thaws, and the usual operating rules and tagging procedures prevail. Hence, the supervised thaws are not as readily applied as those in the other categories; they are reserved for conditions of near emergency.

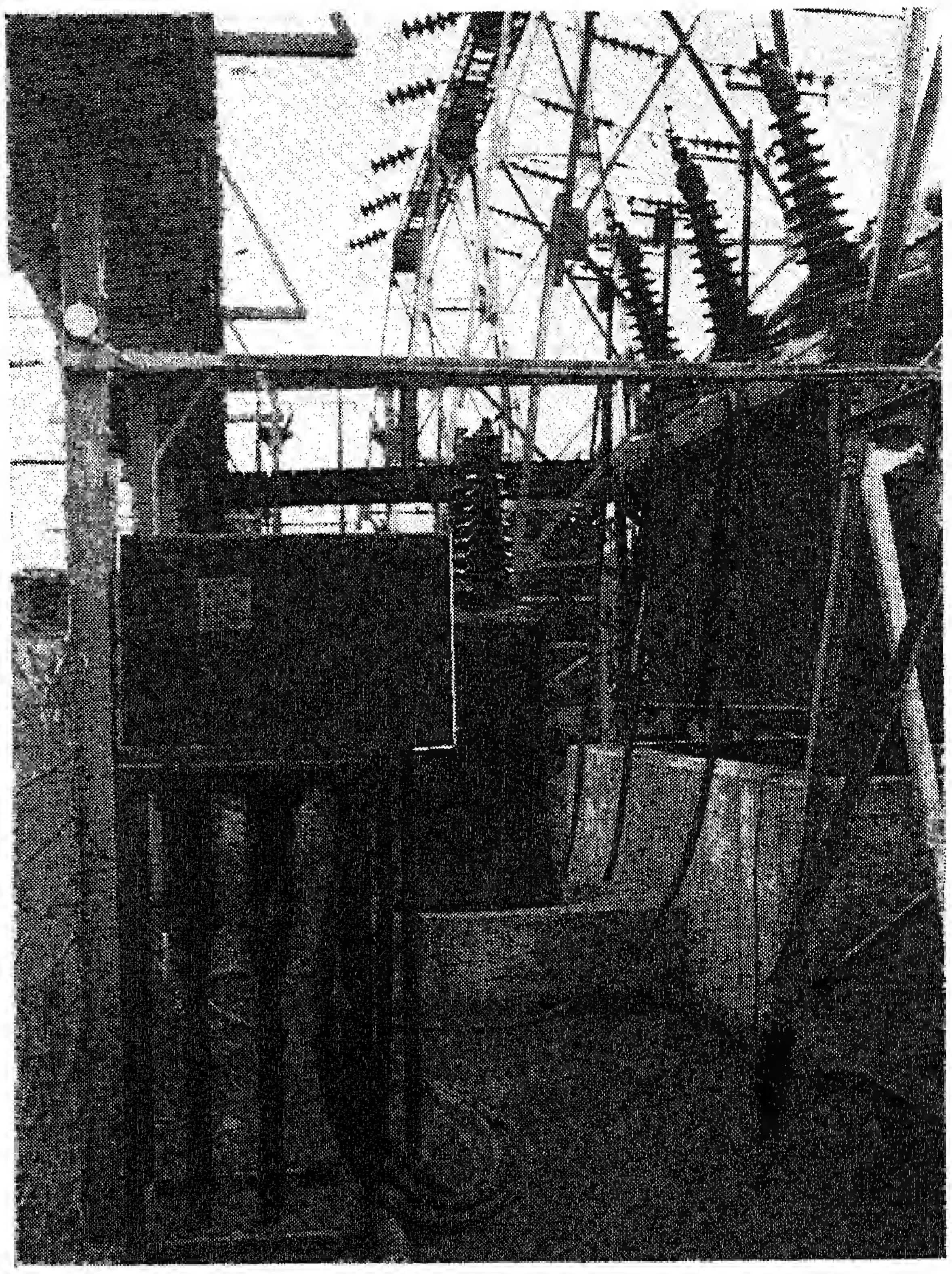


Figure 7. Special safety connection box with jumper cables installed to a high-voltage transmission line

Operating Features

Because of the importance of maintaining strict adherence to the time schedule used in the thawing procedure, the regular station operator on duty plays no part in the operation of the lines being thawed for the obvious reason that nothing must distract from the exact timing and appropriate action called for in the schedule. Off-duty operators are called in on an overtime basis and devote their full time and attention to the thawing procedure. Special sleet-thaw tags (distinguished from the regular protective or blocking tags) are used on the control equipment of the apparatus involved and are placed thereon by a sleet-thawing operator. Such apparatus can be operated only by the sleet-thawing operator, the regular station operator having no jurisdiction over the equipment. Two men must be present when temporary cables or short circuits are attached, the regular operator excluded.

To insure proper understanding of the procedures, sleet-thaw practice runs are held in the early fall, at which time a complete cycle of thawing (4 hours) is conducted without communication and is witnessed by all men assigned to sleet-thawing activity, each at his home station. These sessions have proved beneficial over the years, since they give the newer men a practical demonstration of a procedure which, in the form of written instructions, seems formidable indeed.

Diagrams of the various lines and connections used in the thawing schedule are supplied to all men on the tagging list. A sample of a diagram provided is shown in Figure 6.

Safety Features

Safety to personnel is an important consideration during sleet thawing, due to the emergency and off-routine nature of the operations. At the short-circuiting end of the circuit, safety is assured by installing jumpers on a connection clear of the line and bus and previously tested dead. Safety precautions are more exacting at stations supplying the power for heating the lines. In the larger switchyards, the crew handling the thawing connections is at a considerable distance from and often out of view of the sleet-thaw operator responsible for the operation of the power-supply equipment. In these locations, special interlocks are provided between the thawing jumpers and controls for the power-supply equipment. These features were first installed in 1947.[3]

Interlocking between the yard crew and the controls operator is provided by a completely metal enclosed connection box for attaching the jumper cables from the

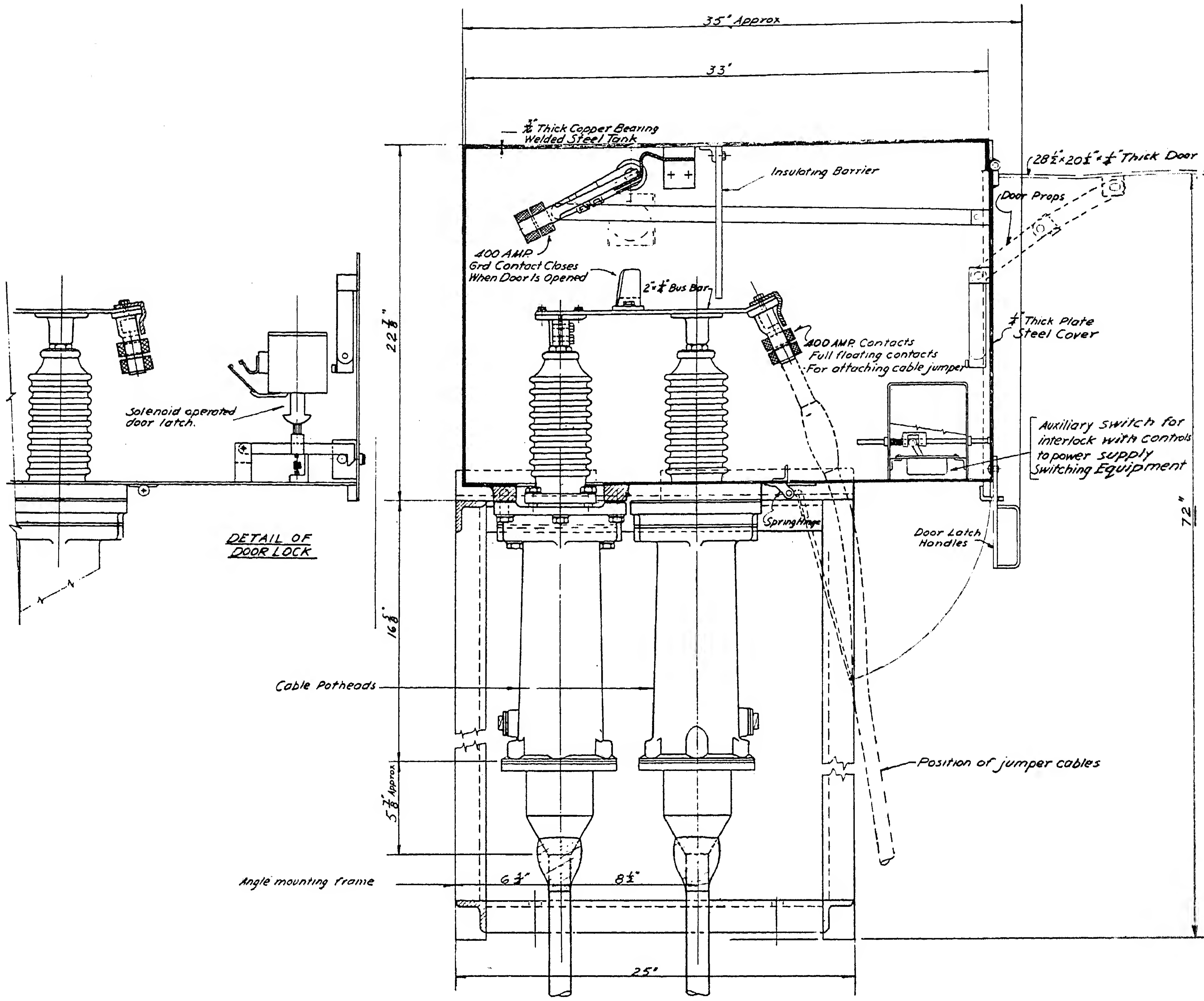


Figure 8. Construction details of safety connection box for sleet-thawing cable jumpers (scale: 3" = 1 – 0")

transmission line to the power supply circuit. These boxes are equipped with hinged front doors which are all required to be in the closed position before the control operator can energize the circuit over which the thawing current is provided. After doing so, all box covers are mechanically locked in the closed positions. When the heating circuit is de-energized, the front doors may be opened again, and during an opening, contacts in the boxes operate to ground the supply circuit and to prevent the control operator from again energizing the circuit.

This specially designed connection box with jumper cables attached to the high-voltage transmission circuit is shown in Figure 7. Details of the box, including grounding and interlocking safety features, are shown in Figure 8.

Another safety consideration is protection to the low-voltage power-supply equipment in the event of accidental contact of a high-voltage transmission line being thawed with a voltage which might damage the insulation of the low-voltage equipment. It is recognized that during thawing operations, accidental contact might be made between the line being thawed and a line in service either by abnormal sags at crossovers, or by out-of-time manual switching at a remote location. Such an accident might impose a severe overvoltage on the transformer or generator being used to supply the thawing power.

Safety to power equipment is provided by the use of spill gaps calibrated to arc over to ground at a safe voltage below the withstand level of the equipment insulation. These gaps are glass-enclosed to assure their constant calibration free of sleet and atmospheric contamination. As the glass is shattered during discharge of the gaps, a protective screen is installed to retain the glass fragments so as to avoid endangering nearby personnel and equipment.

Two sizes of gap assemblies, designed to spill over at 12 kv and 20 kv, 60 cycles, are found to be adequate for power equipment used during present thawing schedules.

Conclusions

The following summarizes the practices which have provided the successful operation of the New England Electric System's transmission lines during the severe ice storms experienced in the New England area.

1. Utilize all available sources of information about weather during sleet-forming conditions.

2. Initiate thawing operations on vulnerable lines in time to prevent an accumulation of ice rather than to melt after an accumulation has formed.

3. Once initiated, thaw lines in accordance

with schedules without dependence on communications among stations.

4. Assure reliability of thawing operations by safety features for both personnel and equipment.

5. Design new lines to withstand maximum expected ice and wind loads without requiring thawing operations.

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Discussion

K. L. Althouse (Pennsylvania Water and Power Company, Lancaster, Pa.): The authors imply that better performance under sleet conditions can be expected from transmission lines using wood pole structures. Two steel tower lines with horizontal configuration designed for 1/2-inch ice, 8 pounds wind, and temperature 0 degrees centigrade, with no sleet-melting facilities provided, have been in service on the Pennsylvania Water and Power Company's system since 1932. There have been several severe sleet storms in this area since that time with sleet accumulations in excess of 3/4-inch radial thickness on these circuits. No trouble has occurred on conductors, ground wires, or structures as a result of these storms.

The authors state that for a 2/0 copper conductor, 350-ampere heating current is sufficient to clear up to 1/2-inch radial ice when the ambient temperature is near the freezing point. Our experience has shown that to insure complete clearing of ice from 2/0 copper conductors, 450 amperes for a 40-minute period are required during the average sleet storm. During serious sleet storms with temperatures of −2 to −4 degree centigrade and high wind velocities, the current must be increased to 485 to 500 amperes to be effective.

Reference is made to detection of sleet accumulation by means of received carrier signal strength. This method has been studied on the 66-kv lines operated by the company. The results were very unsatisfactory, since when no sleet formation is present, fog and mist will cause a change in the attenuation of the carrier signal similar to that caused by sleet formation.

To facilitate application of heating current, it is believed better to provide a special sleet bus with the required disconnectors to each circuit. These special sleet disconnectors should be interlocked with the circuit line disconnectors and with the ground switches.

Installation of special spill gaps to protect equipment used for sleet melting from overvoltage has not been felt necessary on the Company's system. Sleet melting has been a routine procedure on the 66-kv lines of this transmission system since 1915 and no equipment damage due to overvoltage has been experienced. Sleet melting is discontinued when lightning is prevalent. However, since the occurrence of sleet and lightning together is very rare, this is of minor consequence.

O. A. Browne (Western Massachusetts Electric Company, Turners Falls, Mass.): This paper contributes considerable practical experience on a subject of which the theoretical aspects are very difficult to interpret.

Our experience has been that sleet occurs only when conditions are exactly right and the following conditions exist:

1. The temperature of the air is very near 29 degrees Fahrenheit.
2. The fair is stratified and little or no wind is blowing.
3. Sleet is more apt to occur with a rising temperature than a falling one.
4. Conditions above the ground and in the upper air decide whether sleet forms or the precipitation comes down as rain or ice.

The formation of sleet is apt to be confined to a fairly narrow area, often less than a half a mile in width. This feature makes spot checking by reporters somewhat unreliable and the use of loss of carrier signal is very desirable.

The New England Power Company has made ingenious and effective use of every method of getting enough current in the line wires to keep them from coating and also to melt the immediate accumulation.

Our own experience covering 15 years has been confined largely to thawing on a pair of 115-kv lines from Turners Falls to Pittsfield, a distance of 34 miles of 2/0 copper. We have been able to secure about 325 amperes per line by connecting a 15,000-kva transformer directly to the line at 22 kv and short-circuiting the further end by means of gang-operated grounding switches. This requires about 6,000 kilowatt-hours per hour. This is done by sleet operators on a time schedule similar to that used by the New England Power Company.

Our practice has been "When in doubt—thaw." This has required thawing on an average of four to five times during the season from November through March. In 15 years we have had no occasion in which lines gave us trouble if thawing was started promptly and continued until conditions changed.

C. P. Corey, H. R. Selfridge, and H. R. Tomlinson: The authors are grateful to Mr. Althouse and Mr. Browne for their discussions of this paper. Mr. Browne's experiences with and practices in combating sleet are analogous to those of the New England Electric System.

The use of wood poles is one feature of many which have provided an economic protection to our modern lines against destruction by sleet. It is indeed fortunate that wood poles have the combined advantages of providing electrically a high strength to lightning impulses, and mechanically a high resistance to sleet damage, in addition to being a means for an economic construction of lines. Steel tower lines of the New England Electric System with horizontal configuration also have withstood ice loads in excess of their design loadings without thawing precautions. However, the statement that the use of wood poles is one feature which provides an optimum economic protection against destruction by sleet is valid.

The authors recognize the need for adjusting heating current to conditions experienced in other geographic locations.

The use of safety spill gaps for equipment protection has proved to be good insurance against the loss of thawing power equipment by overvoltage. These gaps have been installed primarily to protect against dynamic overvoltages, although they are equally as effective in discharging impulse overvoltages should any lightning occur during the thawing schedules. Their value was demonstrated in one instance when, by a switching error, a voltage of 66 kv was impressed on a line being thawed with a power supply from an 11-kv generator. The gaps operated to trip the 66-kv line without damage to the generating equipment.

The authors agree that an attenuation of the carrier signal needs an interpretation based on prevailing conditions. However, during sleet-forming weather, it provides another source of information which alerts the operators and by which the general conditions may be appraised.

A Passive Compensator for Voltage Flicker

PAUL A. CARTWRIGHT
MEMBER AIEE

MOTOR starting on secondary distribution systems frequently results in undesirable lamp flicker. A drop of two volts will produce a perceptible lamp flicker for a 120-volt 100-watt tungsten filament lamp.[1] There are numerous cases of rural customers located at the end of long primary extensions where motor starting and running produce objectionable reductions in the utilization voltages.

Lighting systems are not the only type of loading which may be subjected to voltage surges due to switching of other parallel loads. Many sensitive experimental laboratory circuits require constant or nearly constant voltage sources with a minimum of wave-form distortion. An elevator operating from a secondary system supplying other loads can cause an often-repeated and annoying voltage disturbance, as was the case in the circumstances described in this paper.

The electrical engineering laboratories of the University of Minnesota are supplied from a 3-phase transformer bank of single-phase units. The building freight elevator motor is also supplied from this same bank. The effect of the elevator operation on the laboratory supply system was recently made more pronounced by a change in the building service from a 120/240-volt delta bank to a 120/208-volt Y-connected bank. The laboratory utilization voltages are 240 volt 3 phase, 120 volt 3 phase, and 120/240 volt single phase. The change-over of several campus buildings to a 120/208-volt service necessitated an additional transformer bank between the main vault and the electrical engineering laboratory distribution panel. The diagram of Figure 1 shows the present arrangement used to feed the electrical engineering laboratory system.

Previous Developments

A wide variety of voltage regulators and stabilizers have been devised for application on distribution systems. Step-voltage regulators are in common use on primary distribution systems but are of little value for local voltage regulation where the disturbing element is a motor or any other parallel load. Application of such a regulator at the load location to control the secondary voltage is costly and because of its relatively slow response it cannot eliminate the transient voltage drop.

Synchronous-synchronous motor-generator sets are sometimes employed for laboratory service and may provide an excellent stable voltage source. They are bulky and expensive, however, and unless an extremely stable source is required are not economically justified.

The electronic voltage regulators now commonly employed are generally quite satisfactory for the supply of relatively small loads. For applications requiring the elimination of local switching surges but where some long-time voltage drift can be tolerated, the electronic regulator, may not be feasible economically. This would be particularly true where loads as large as 50 kva are involved.

Another method of voltage regulation involves the insertion of a fixed value of voltage boost in a circuit at the time of load switching. This is somewhat inflexible and does not always allow for variation in the magnitude of the load being switched.

Automatic voltage compensators have been devised for resistance welding control.[2] Most of these devices do not actually regulate the line voltage but are used in conjunction with phase-controlled welding circuits to advance or retard the firing voltages of ignitron-type tubes.

A static voltage regulator utilizing series and parallel combinations of capacitance and inductance has found some application on single-phase circuits and on 3-phase 4-wire Y systems.[3] This type of regulator employs a nonlinear reactor and limits voltage changes to ± 0.5 per cent for a 30-per-cent variation in line voltage. It has an efficiency of about 90 per cent in a 1-kva size and produces considerable and undesirable wave-form distortion at light loads.

An automatic voltage regulator utilizing ferroresonance has been patented by P. H. Craig.[4] This device does not regulate the whole voltage but uses a small booster transformer and regulates only a small part of the total voltage.

Series capacitors can be used to improve steady-state voltage stability, but transients are not suppressed and they present some protection problems. The possibility of self-excitation and resulting instability of induction motors fed from lines containing series capacitors must also be considered in such applications.[5]

All of these voltage regulators produce a more or less constant load voltage as the

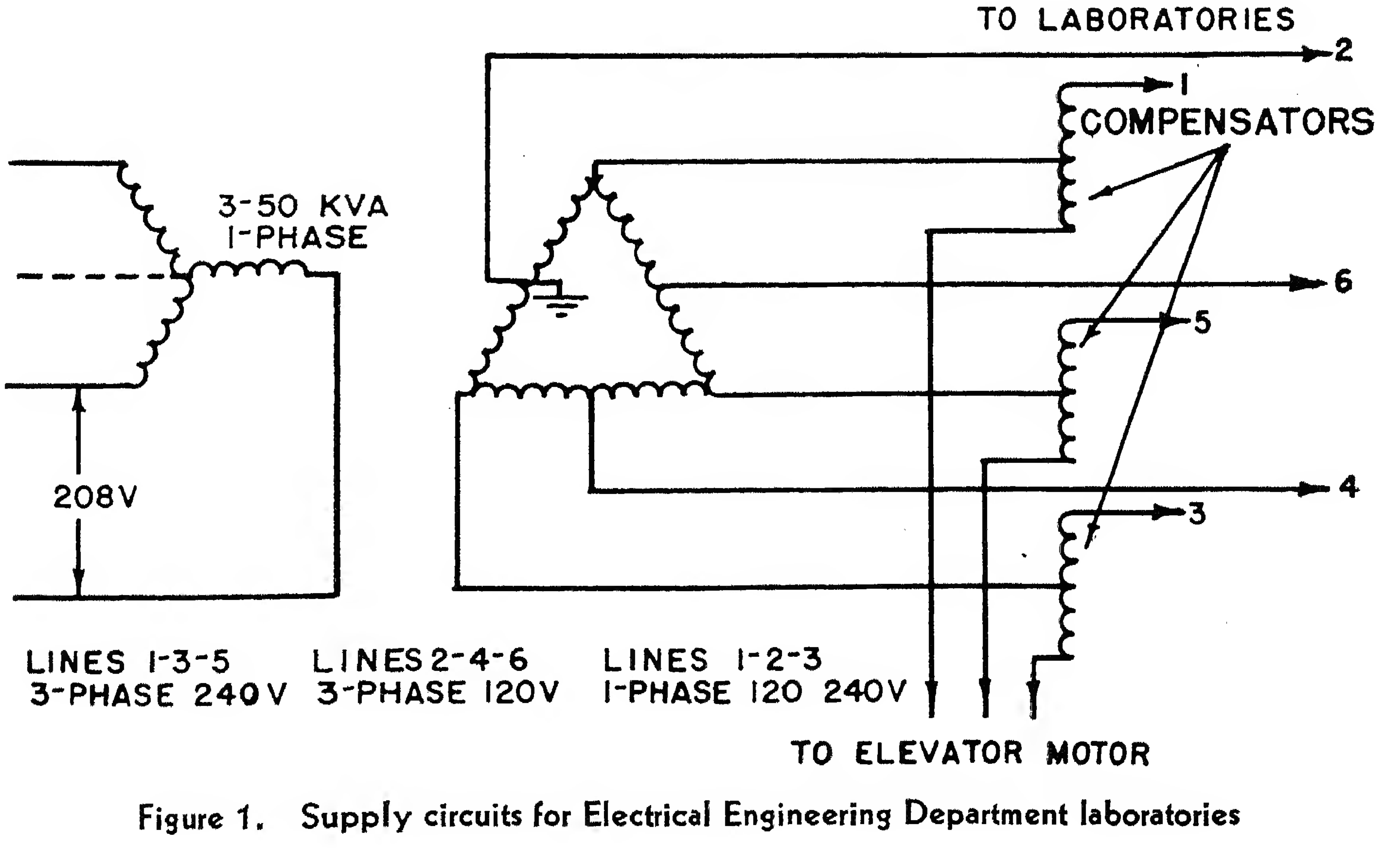

Figure 1. Supply circuits for Electrical Engineering Department laboratories

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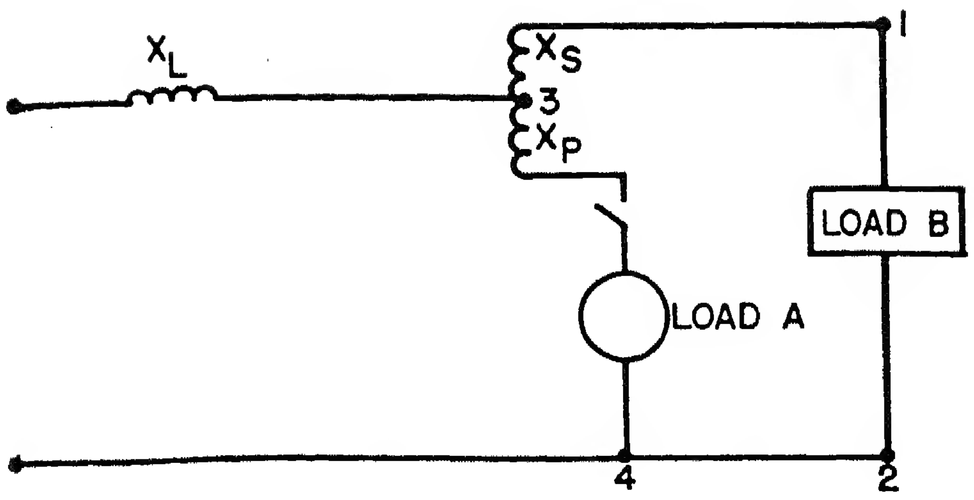
The writer wishes to express his appreciation to Dr. H. E. Hartig, Professor and Head, Department of Electrical Engineering, University of Minnesota, for suggesting the use of the device described in the paper and for his helpful comments during the design and installation of the units.



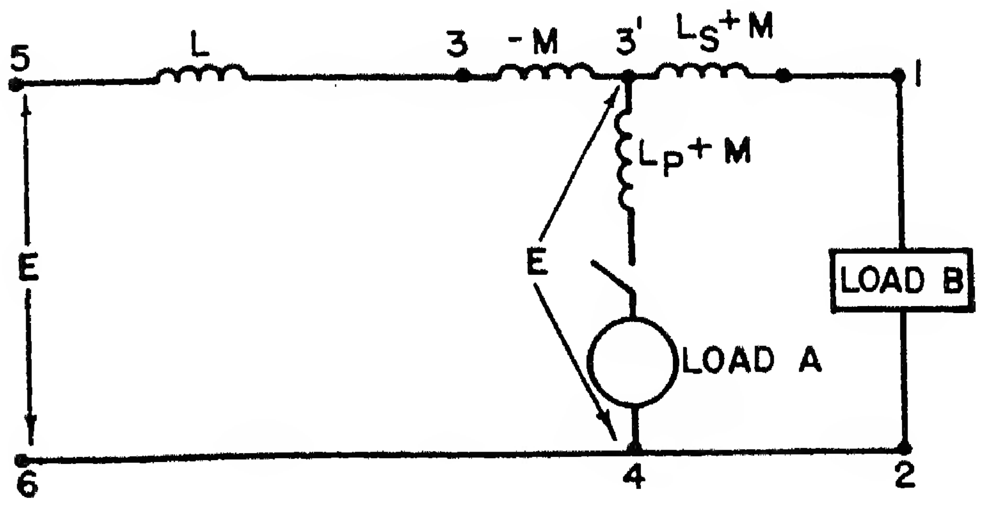


Figure 2 (upper left). Compensator application on a single-phase circuit

Figure 3 (lower left). T-equivalent of a compensator applied to a circuit having inductance only

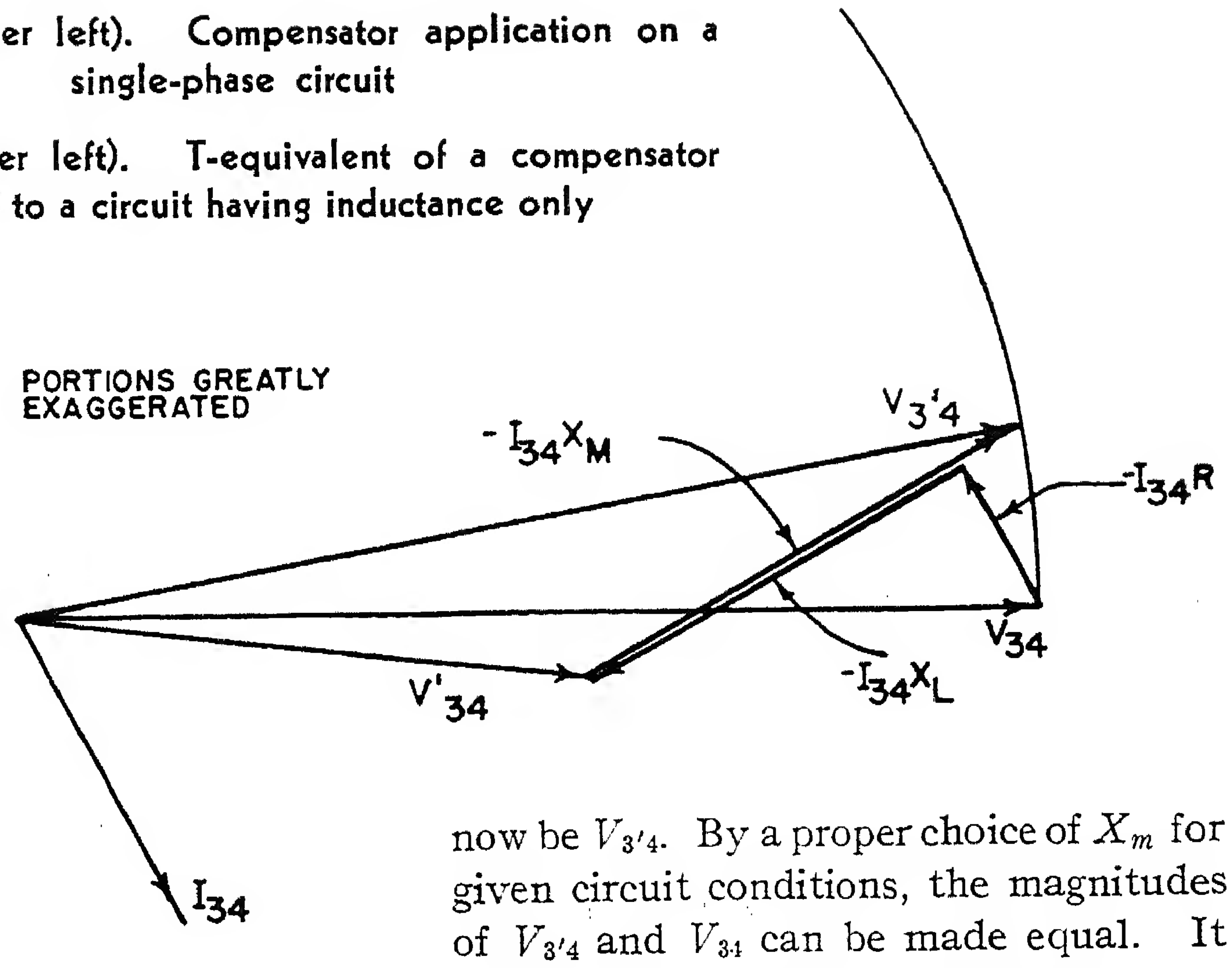


Figure 5 (right). Current and voltage relations involved in compensator application

load itself is varied. In certain applications it may be allowable to permit a drop in the voltage supply to the disturbing load and to compensate only for the voltage reduction of the associated load.

For combined lighting and motor loads, or for any combination where the switching of a portion of the load causes disturbance on the remaining load (assuming that the power supply itself is steady), a voltage regulator consisting of a very simple transformer device may be used to eliminate voltage drops to the steady loads associated with switched loads.

Theory of the Passive Compensator

The analysis of the inductive line-drop compensator is carried out easily by means of the T-equivalent for the transformer, Figure 3, in which there is no inductive coupling between $-M$, L_s+M and L_p+M. To achieve perfect compensation for the line drop caused by L it is necessary only that the mutual inductance $-M$ of the transformer be made equal to the line inductance L. Then $L-M=0$ and no voltage drop takes place between terminals 5–6 and 3′–4. It is thus seen that the two loads connected to 3′–4 are connected to a bus of zero internal impedance and consequently the loads cannot interfere one with the other. If the loads are approximately of the same size, L_s and L_p appropriately may be made equal and if the coupling between the transformer coils is very close, the mutual inductance M will be only slightly less than $L_s=L_p$. Under these circumstances the design takes the form of a center-tapped autotransformer, with the center tap connected to the line whose reactance is to be compensated.

When the source from which power is drawn contains resistance, as well as inductive reactance, see Figure 4, the problem of compensating for line drop is not only more complicated but it can be achieved only in part. If the magnitude of the load voltage is to be maintained constant, some shift in phase must be allowed. However, a useful degree of compensation is possible over a fairly wide range of load and power-factor values.

The current and voltage phase relations involved in line-drop compensation are shown in Figure 5. This diagram relates only to the effects of the current taken by one load on the voltage of its associated load. The open-circuit voltage V_{34} of Figure 4 is taken as the reference. When a current I_{34} is supplied to load A and no compensation is made for the line drop, the voltage across points 3–4 and 1–2 becomes V_{34}'. With the same current supplied to load A, the use of a line-drop compensator would provide a compensating potential of value $-I_{34}X_m$. The resultant voltage across points 3′–4 would now be $V_{3'4}$. By a proper choice of X_m for given circuit conditions, the magnitudes of $V_{3'4}$ and V_{34} can be made equal. It is, of course, possible to design a compensator unit with a value of X_m such that $V_{3'4}$ is either greater or less than V_{34} for stated values of line impedance, load current, and power factor.

Single-Phase Compensation

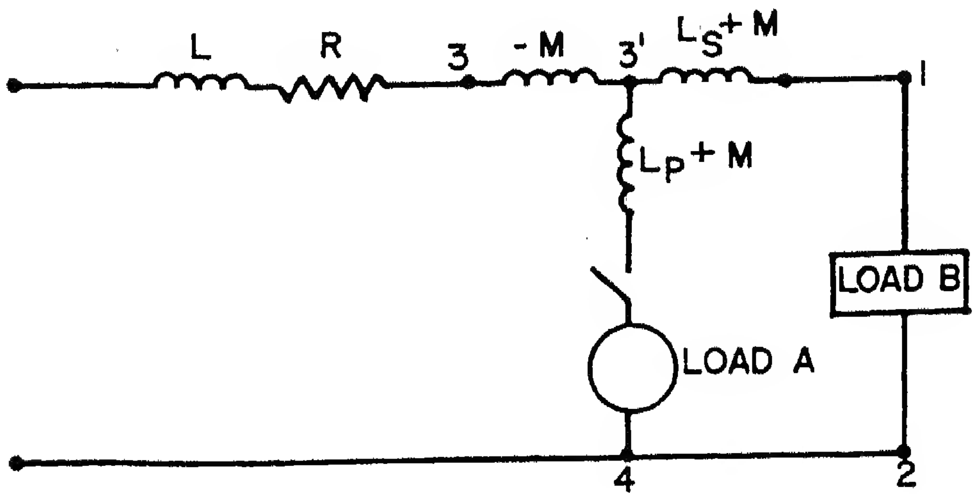
By way of preliminary experiment a small compensator unit was designed for use with a 5-horsepower 110-volt single-phase repulsion-induction motor. This motor was operated from a 120-volt circuit and when placed directly across the line reduced the voltage from 120 volts to 110 volts during the starting period. Application of one compensator unit occupying a volume of only 32 cubic inches reduced the voltage fluctuation to less than 1 volt.

Application to a 3-Phase System

The problem dealt with in this paper was brought into focus by a disturbance caused by a 3-phase elevator motor to certain constant loads which were very sensitive to voltage changes. The motor is a 22-horsepower 220-volt wound-rotor machine which has an operating current of approximately 60 amperes at a power factor of 60 per cent.

Let us consider this special circuit and associated loads. Referring to Figure 1, the source of the utilization voltages was a transformer bank of three single-phase 50-kva transformers, and the disturbing

Figure 4 (below). T-equivalent of a compensator applied to a circuit having resistance and inductance



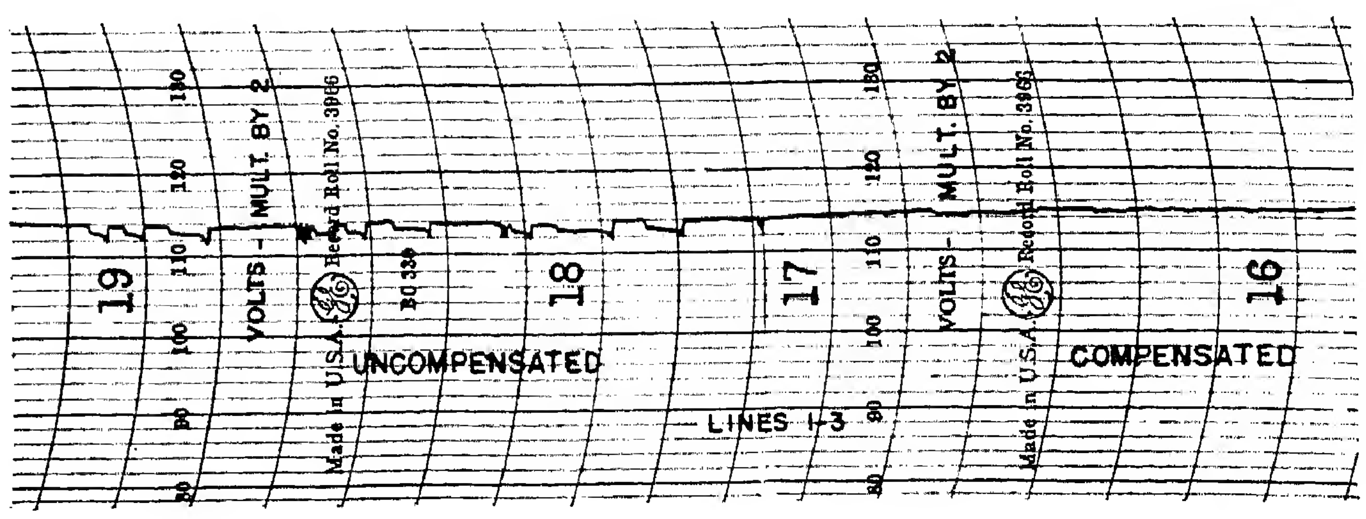


Figure 6 (right). Voltage chart showing reduction of voltage fluctuation achieved by application of line-drop compensator

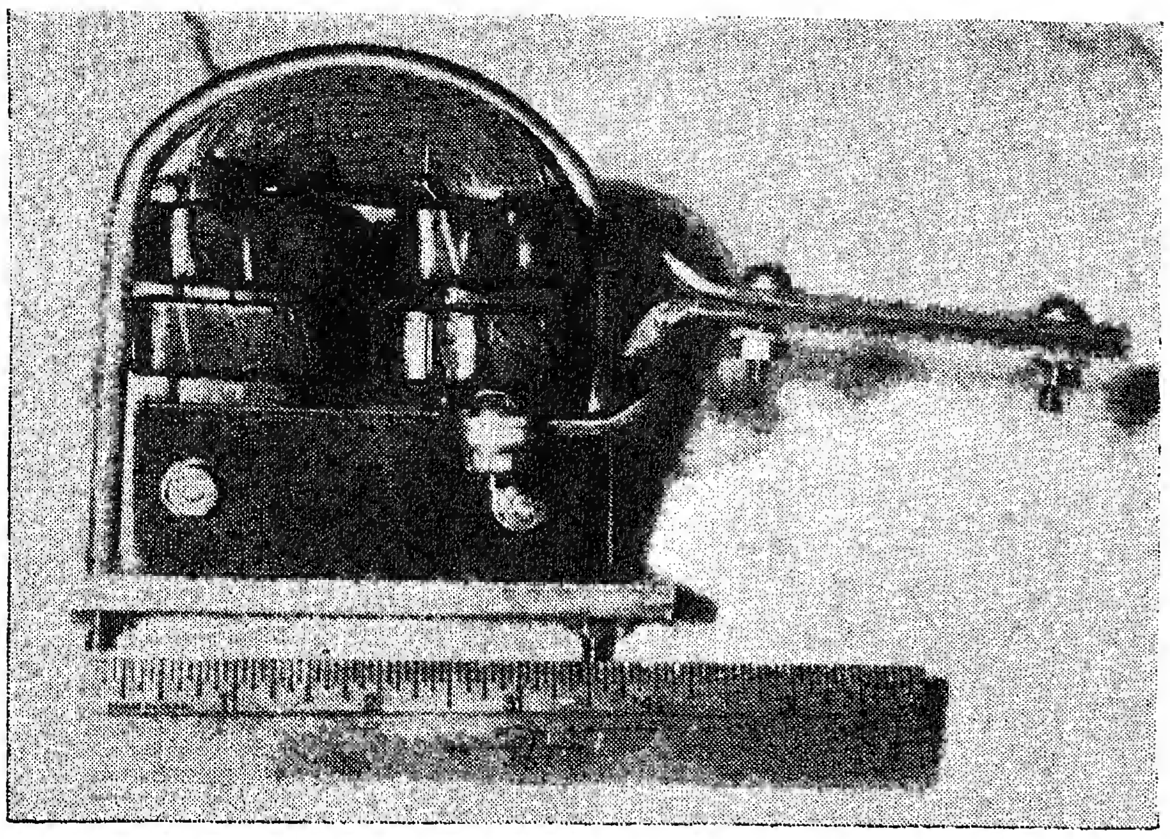


Figure 7 (left). Compensator unit designed for use with a 22-horsepower 220-volt induction motor

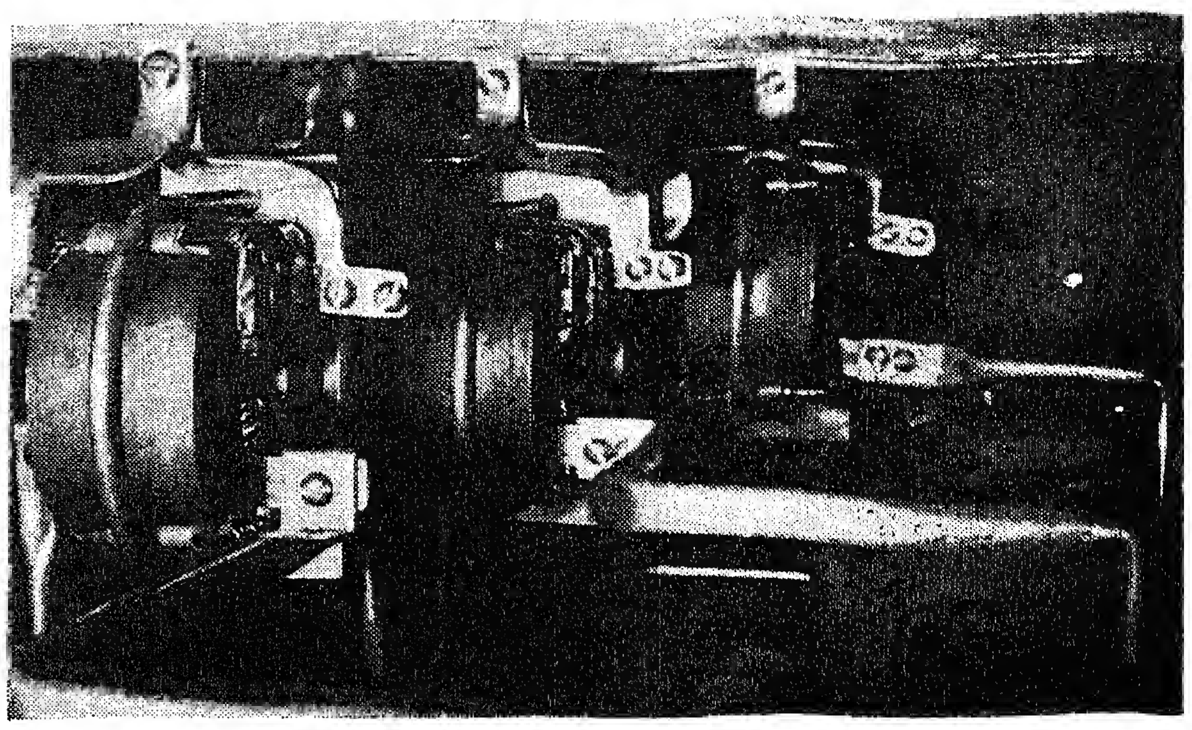


Figure 8 (right). An installation of line-drop compensators on a 3-phase system

load was the elevator motor. The voltage sensitive loads which were to be protected against voltage fluctuations were the various research and experimental laboratories supplied from lines 1–3–5. The use of passive line-drop compensators such as described in this paper proved to be a satisfactory solution to this particular problem of voltage fluctuation.

Design and Application

In order to carry out the design of the compensators for this installation on something other than a cut-and-try basis, the supply system resistance and reactance per phase were determined, and measurements were made to determine the starting current, running current, and power factor of the elevator motor. The design took the form of a center-tapped unit and the assumption was made that $X_p = X_s = X_m$. The required value of X_m was calculated to be 0.05 ohm, which can be shown to give good compensation during the starting period as well as during the running period of the elevator motor. The voltage chart shown in Figure 6 illustrates how effective these compensators are in reducing voltage fluctuations.

In the design of the unit some balance between magnetic circuit length, cross section, and saturation must be obtained. An exciting current of 110 amperes during the motor starting period dictates the use of an air gap, the alternative being a magnetic circuit of disproportionate length. The air gap allows a very convenient means of adjustment of the compensators for operation with various types of loads after permanent installation.

A single unit is shown in Figure 7, and the final installation in Figure 8. The average length of the magnetic circuit for each core is 9 inches and the cross-sectional area 1.5 inches. An air gap of 0.016 inch gave very good operating results for the particular power source and motor involved. The coils were wound of copper strap and the windings divided between the two core legs. The over-all dimensions of each unit are 4 inches by 4 inches by 4 inches, exclusive of the bus connection, and each weighs $5^1/_2$ pounds.

Conclusions

Voltage fluctuations produced at one load by the switching of another associated load can be greatly reduced by the use of a simple transformer device herein described as a line-drop compensator. For supply lines whose impedances are principally inductive with relatively small resistive components, near-perfect line-drop compensation can be achieved. The compensation applies not only to steady-state but also transient disturbances. Where the line resistance is not negligible, good steady-state compensation is still possible over a wide range of load current and power factor values.

One of the most attractive features of this type of compensation is the simplicity, compactness, and low cost of the unit. It may be expected that little maintenance will be required on this device since it is simply a low voltage autotransformer.

Nomenclature

X_L = inductive reactance of distribution system
R = resistance of distribution system
X_s = inductive reactance of compensator winding in series with load B
X_p = inductive reactance of compensator winding in series with load A
X_m = mutual reactance of compensator unit
V_{12} = potential drop across load B
V_{34} = open-circuit voltage at load point
V_{34}' = voltage on lines with no compensation
$V_3'_4$ = voltage on lines with compensation
I_{34} = current taken by load A

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4. An Automatic Voltage Regulator Without Moving Parts Employing Ferroresonance, **Palmer H. Craig.** *AIEE Transactions*, volume 58, 1939, pages 830–36.

5. Self-Excitation of Induction Motors with Series Capacitors, **C. F. Wagner.** *AIEE Transactions*, volume 60, 1941, pages 1241–47.

Discussion

W. C. Wegner (Northern States Power Company, Minneapolis, Minn.): This is a very interesting application and could prove to be of considerable value in many instances. One logical question arises, however, What happens to the voltage regulation on each load as the load itself is varied? It would appear that as a result of the installation of compensator units the input impedance would be increased at each load point and in the case of a center tapped unit the line reactance would be approximately doubled.

P. A. Cartwright: The question raised by Mr. Wegner is naturally of concern in the compensator application. In the case of unity power-factor loads, such as lighting, doubling the line reactance affects the voltage regulation very little. The voltage regulation on motor loads would be affected somewhat, but this would not be a critical matter in many cases. Thus the use of passive compensators with combined motor and lighting loads, where the loads can be split and each fed from a compensator leg, should prove quite satisfactory. In other cases the load being subjected to voltage flicker may itself be of a relatively high impedance and an increase in the line reactance would be of no concern insofar as its own voltage regulation is concerned.

One might consider a rather general case where a load of value P is being supplied from a line of inductance L. If a center tapped compensator unit is now employed with $M = L = L_p = L_s$ and the load equally divided between each leg, then each portion of the original load will see approximately $2L$ as the line inductance. But since each load portion is now only $P/2$ the voltage regulation on each portion will be essentially the same as it was originally on the total load P.

Power-Line Carrier for Relaying and Joint Usage

Part II. A Survey of Modern Power-Line Carrier Systems

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MEMBER AIEE

B. WADE STORER
ASSOCIATE MEMBER AIEE

THIS paper, the second of two, is intended to emphasize the relaying techniques involved in the subject. The first paper[1] discussed the carrier tools available, their respective advantages and limitations, and some of the control problems introduced by joint usage for nonrelaying functions.

Principles and Limitations

There are three recognized principles used in carrier relaying, namely:

1. Transferred signals, which are used to trip remote circuit breakers directly, or to expedite such tripping.
2. Phase comparison by a carrier pilot channel.
3. Directional comparison, which also is referred to as the blocking type.

Any of these principles must be applied with attention to the local conditions under which it must function. Thus, inattention to features of detail design, as in trip and control circuits, in some cases may hold lines, equipment, or protection schemes out of service unnecessarily, and in others will provide the operating personnel with numerous special features to remember and watch. Such instances, plus random difficulties, can build up antipathy to any type of relaying, carrier included.

The present standard forms of items 1, 2, and 3 will not be discussed in detail, but the present common usages and limitations follow.

Transferred Signals

To date, these have been used principally for protection of terminal equipment. In some installations considerable transmission-line protection has been obtained. These by-product operations frequently outnumber those for terminal equipment failures.

In principle, at one line terminal the primary relays must, without remote assistance, recognize a fault condition and transmit a signal to one or more remote terminals to trip or expedite the tripping of remote circuit breakers. The remote terminals always must be receptive to these transferred signals, hence there is a continuous exposure to spurious signals. Such signals may originate from system noises or defective carrier equipment or combinations of the two. To minimize this exposure, or the effects of it, the following safeguards have been used:

1. Highly selective receivers.
2. Time delays at the receiving end.
3. Coded signals.
4. Continuously transmitted holding or restraining signals.
5. Fault detection at the receiving end, that is, the circuit breaker tripping is expedited, but not completed, by a received operating signal.

The presently accepted means of transferring signals are: (a) narrow-band frequency-shift signals;[2] (b) audio tones by amplitude modulation with safeguard 2 or 4, or both. Audio tones also can be transmitted by single side-band or frequency modulation, perhaps with some gain in reliability.

The unique feature of transferred signals is the continuous exposure at the receiving end, safeguard 5 possibly excepted. In contrast, phase-comparison and directional-comparison schemes are active only a few seconds out of a year, hence offer entirely different problems in reliability because of their long inert periods.

Thus, a receiver used directly for circuit-breaker tripping is something like a safe with an unknown combination. It could be opened by random twirling of the knob, perhaps quickly, perhaps after an indefinitely long time, but there is always a finite probability, however small, that it will be opened in a given time. Similarly, there is always a possibility that a spurious trip signal will get past the safeguards. Assume that a given scheme will trip on a spurious signal once in 100 years (for present schemes this is probably optimistic). A large utility with 100 installations could expect then a random trip once a year on the average, and two or three in one year should not surprise anyone, but would probably do so.

To date, it has been questioned as to whether transferred signals for transmission-line protection channeled over the protected line can get past an intervening fault. There are little actual data on this. A rather limited experience indicates to the authors that the signals do pass by faults. Can others corroborate this from experience or tests? Alternate signal transmission paths are used in a few cases. These may be obtained from alternate lines either independently, or by intercircuit coupling. Microwave transmission is an obvious alternate.

To give high-speed protection to a transmission line by transferred signals, the trip signals must be initiated quickly for faults anywhere on the line. This means that every fault must be in at least one first (instantaneous) zone. For interphase faults this is readily attainable except for short lines and a few special cases of multiterminal lines. For single line-to-ground faults it is not common practice to provide such primary relaying. High-speed, independently acting ground relays have a tendency to be rather complex, but a more important reason for the lack of ground relay progress is that phase- or directional-comparison carrier relaying has been available to provide the desired quick action—up to the stage where the line problems get beyond the basic capabilities of either type. If transferred signals are to be developed further, some added attention will need to be given to the relays which initiate the signals.

Phase Comparison

A carrier channel is used to transmit pulses corresponding to components of line current as registered at remote terminals in order that the local and remotely transmitted components may be compared as to time phase status.

The comparison could be made continuously for each phase wire of the line, but it is not American practice to do so. In France continuous comparison on individual phases is practiced. If redesigned carrier or microwaves should give

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G. W. Hampe and B. Wade Storer are both with the Commonwealth Edison Company, Chicago, Ill.

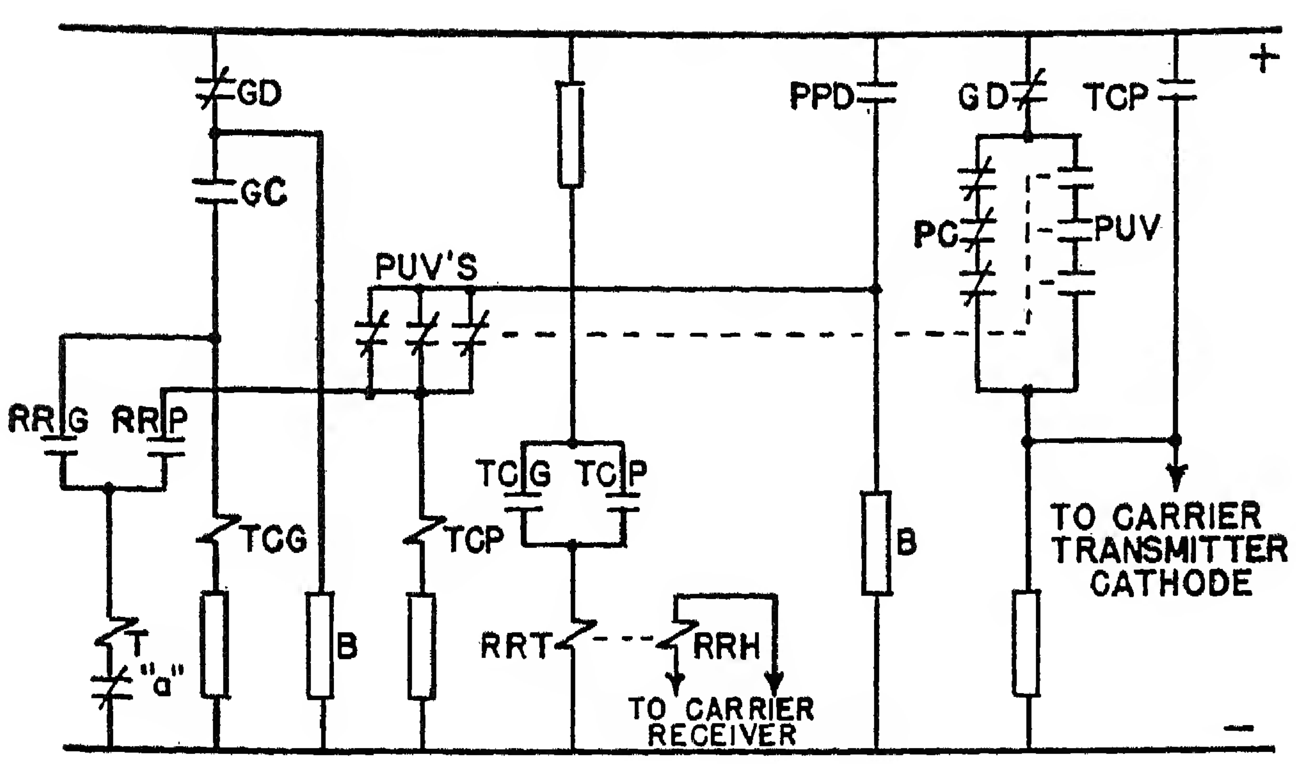


Figure 1. A directional-comparison scheme for a short 3-terminal line

PC—phase current relays
PUV—phase undervoltage relays
PPD—polyphase directional relay
GD—ground directional relay
CG—ground current relay
RRH, RRT—receiver relay coils; H—hold, T—trip
RRG, PRP—receiver relay contacts; G—ground, P—phase
TCG, TCP—trip contactors; G—ground, P—phase
B—bleeder resistors (to activate contacts)
T—trip coil

us sufficient channels,[5] it would be interesting to attempt similar adaptations, which would avoid some of the functions which American practice must provide, namely:

1. Reducing polyphase line currents to single-phase quantities.

2. Setting a level for initiation of signal transmission.

3. Setting a level for comparison to begin.

4. Setting an angle of discrimination.

5. Providing tripping action on an unfavorable comparison (internal fault or no remote signal).

These functions are combined in several ways, resulting in several schemes, with one thing in common: An attempt at a given terminal to compare with a nonexistent remote signal will result in tripping when the local line current components exceed certain limits. The coordination of function 2 with 5 puts limitations on the sensitivity of the scheme.

Consider protected lines having temporary large currents for the prevention or melting of ice coatings. Function 2 would normally be set above normal load, but not above ice prevention current.

Case I. The operators have advance warning that line currents above the signal initiation level are due. Some or all of the following options then should be available:

a. To operate with continuous pulse transmission. See Case II for a change to make this much safer.

b. To raise the initiation level of the scheme. This can be done by shifting to higher ratio line current transformer taps, which also prevents overheating of relay and meter equipment and keeps indicating meters on scale so that current levels can be watched. The authors do not know of any such provisions now in service, but recommend them.

c. To eliminate positive phase sequence components of line current from comparison. Load currents then will be avoided in phase comparison. The combination scheme in a recent paper[3] does this inherently, but barring this or (b) it should be put on the operator's control switch.

d. To take the scheme out of service. A control switch for this is standard, and by modern standards its first off-normal position should open only the trip circuit. Suspension or limitation of carrier initiation as for (c) then can be on a subsequent position, including trip circuit restoration.

Case II. An operator finds that the scheme is transmitting continuously. On present schemes, when this occurs, he no longer can test the strength of the carrier signal received from the remote end (unless double frequency operation is used with wide spacing). Furthermore, he has lost the push-to-talk communication channel with respect to the remote terminal, and may have no alternate. Thus, his safest procedure is to open the local trip circuit and ask the power supervisor to unload the line to less than the transmission dropout.

If he could watch the remote signal strength, he would be able to use more discretion. There are gaps between the pulses of locally transmitted carrier. It is thus theoretically possible to meter the remote signal in these gaps, and it may well be quite practical to develop such metering. If this is done, Case II becomes analogous to Case I.

Developments such as the one just described would extend the applications of phase comparison somewhat, as it would be practical to operate closer to expected loads and to keep protection on the line during ice storms and during the subsequent ice melting procedures.

Directional Comparison

This class of carrier relaying has undergone the most development of the three classes, or at least it has the most numerous variations around the same basic principles. Fundamentally, directional elements, which may be polarized by current or voltage, are to indicate whether the fault is on the line or elsewhere. A flow of current out of the line at one terminal tends to indicate an external fault, and that terminal sends carrier (or a modulation thereof) to restrain the remote terminal or terminals. For faults on the protected line no terminal should transmit, so that sensitive local elements can set up tripping without restraints.

Conventionally, distance-type relay elements are used to control carrier for interphase faults, that is, the directionality is supplemented by distance measurement. Since the same elements usually have other functions, compromises must be made. For example, the non-carrier directionality of the third zone may be reversed or abandoned. Also, it may be necessary to lengthen one or more settings into the region of tripping on load or load swings. Thus, in some instances the third zone may be limited to carrier control only.

Considering the two types as used currently, directional comparison in general could be kept in service up to higher values of sleet prevention current than phase comparison relaying. The difference is offset in two respects:

1. The compromises in settings for directional-comparison carrier control tend toward greater load sensitivity on backup elements.

2. Backup elements trip without warning. In contrast phase comparison can be expected to warn by initiating carrier without tripping.

When line currents are set up high enough to melt ice, as distinct from prevention, present practices are to leave some lines without protection or to bring into service special relays. Undervoltage and neutral relays are used, as they are not dependent on line current transformers. If provision should be made to use special current-transformer tap ratios as proposed here, both phase and directional comparison would have greater ranges of usefulness.

In directional comparison, the carrier ground relays conventionally control the same signal as the phase relays and may be given more or less preference in con-

trolling it. These relays are affected by spurious ground fault currents and voltages, such as those resulting from zero phase sequence coupling to other lines, and from line and transformer phenomena in the closing and opening of circuit breakers. The zero phase sequence induction can be offset by potential polarization.

The phenomena on closing and opening circuit breakers will be designated as transition phenomena, and will be divided into two classes:

1. If circuit breakers are not operating as intended, the pole times may differ by cycles or even seconds.

2. If circuit breakers are well adjusted, the electrical properties of the system, with attached instrument transformers, nonetheless yield quite a range of phenomena. Thus, if three poles open at current zeros or close at voltage maxima, they will be 120 and 240 electrical degrees apart. This is ideal switching, with minimum, but not zero, transition phenomena. The other extreme is shown in an oscillogram which was presented in the discussion of an AIEE Transactions paper.[4] In that instance apparently all poles closed in 120-degree sequence, but at voltage zeros. The results were rather surprising. Most switching is somewhere between the extremes. It is the opinion of the authors that directional comparison is more susceptible to transition difficulties than phase comparison. They do not imply that the latter is immune.

For increased security of directional comparison against circuit-breaker transitions there probably should be a full set of potential transformers on each line at each terminal (connected star-delta). For economy, these are not always provided. An alternative to this would be the use of a combination of phase-comparison and directional-comparison schemes, as proposed in a recent AIEE paper.[3] If directional comparison is limited to interphase faults, a V V set of potential transformers may suffice for several lines on one bus. Phase comparison is thus to be used for ground faults only, minimizing the difficulties on load currents and spurious ground fault currents.

Lines which are too short for the application of distance relays are a special problem. Because of physical barriers or high induced pilot-wire voltages, pilot-wire relaying may not be practical. Phase-comparison carrier relaying may be practical for short 2-terminal lines, but not always for 3-terminal lines. In seeking the solution of such a problem the directional-comparison scheme of Figure 1 was projected. It was never installed, as the line layout was changed. In this scheme directionality is supplemented by undervoltage, instead of distance. Hence, it is ap-

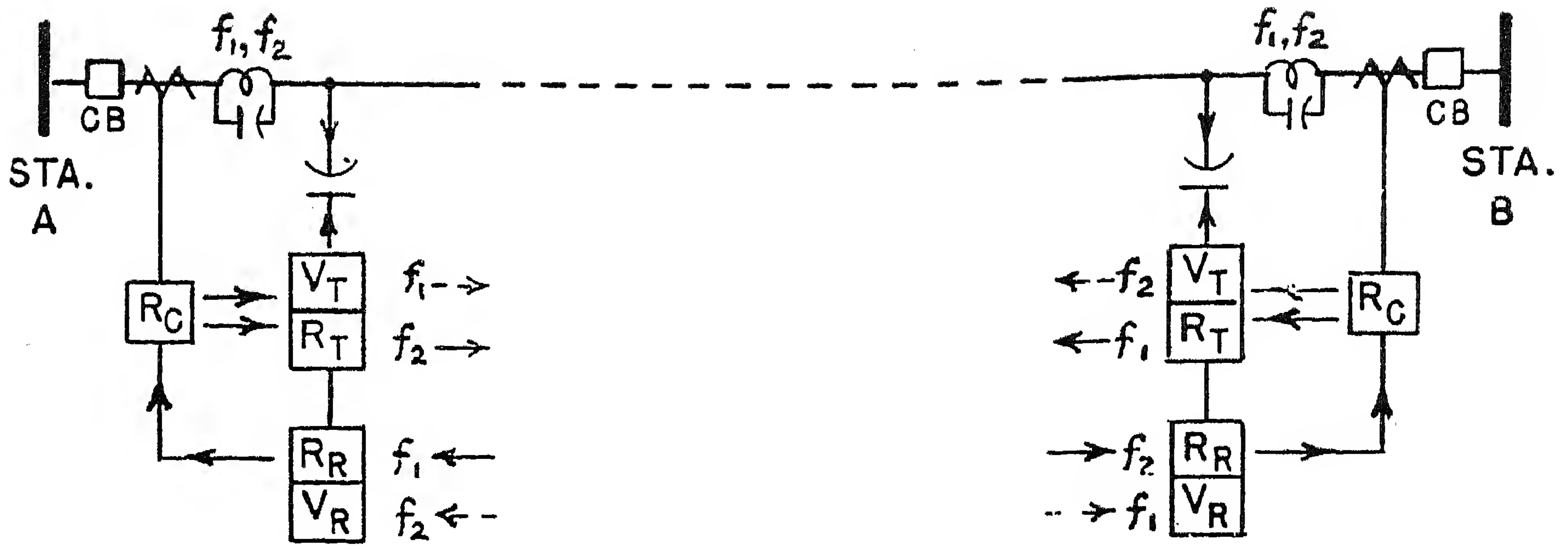


Figure 2. Joint usage by frequency inversion

V_T—voice (or other non-relay service) transmitter
R_T—relay transmitter
R_R—relay receiver
V_R—voice receiver
R_C—relay controls (phase or directional comparison)
f_1, f_2—basic carrier frequencies

plicable to any line on which the voltages at the various terminals do not diverge much during faults, that is, a short line or a somewhat longer one with low fault currents. The contacts in Figure 1 are positioned for a de-energized network. The undervoltage relays are to be connected interphase, on line potential transformers, and are to be set to ignore single line-to-ground faults. The phase current relays are to be set 10 or 20 per cent of full load and deactivate carrier when the line is de-energized. The scheme is designed to avoid timing races between contacts at a given terminal, which is a desirable feature for any directional-comparison scheme.

The carrier spectrum is crowded, and is becoming more so. One remedy is more selectivity in carrier receivers. This can be done for directional comparison by substituting two frequency shift channels for keyed carrier as in Figure 7. In general, non-relay usages cannot be compressed in this way, nor can phase comparison. Therefore joint usage of channels seems inevitable, as in Figures 2, 6, and 7. This adds the advantage of continuous monitoring of relay channels, but also introduces several special problems.

Special Problems of Joint Usages

It was pointed out in the first paper that the facilities of relay carrier could be used jointly in varying degrees for other purposes, such as communication, sleet detection, and so forth. In practice, the installations in which there is absolutely no joint usage of relay carrier facilities for other purposes are quite rare.

If a relay carrier transmitter is functioning for other purposes, it may be rather difficult for the primary relays to recover control of the relay channel. The primary relays usually are actuated by fault current, but in some cases a transmitting terminal furnishes little or no current to the fault. Hence, the non-relay carrier is transmitted and blocks effective relaying. An open line circuit breaker produces one form of lost transmitter control, although there are at least six other possibilities of having weak or zero fault current.[1] The first paper[1] included an oscillograph record showing failure to recover control of a continuous signal set up for sleet detection, even though both line circuit breakers were closed at the start of fault.

All methods of providing priority for relaying under joint usage can be grouped under avoidance of the relay signal, augmented control, and combinations of the two. Augmented control includes:

1. *Circuit Breaker Interlocks.* Non-relay carrier is to be removed by auxiliary contacts on line circuit breakers. Since open line circuit breakers are not always involved, this does not secure complete control. Also, it suspends outbound communication past an open circuit breaker, at a time when it may be needed.

2. *Undervoltage and Zero Phase Sequence Voltage Controls.*[7] Granting a complete set of line connected potential transformers (which is not always true), the control functions are:

(a) shutting off non-relay carrier during faults;

(b) restoration (with time delay if automatic) of non-relay carrier on a de-energized line;

(c) immediate return to status quo when the dead line is reclosed to a live system.

Avoidance of the relay signal includes:

3. *Relaying on an Audio Tone.* Short of microwave frequencies used as a carrier

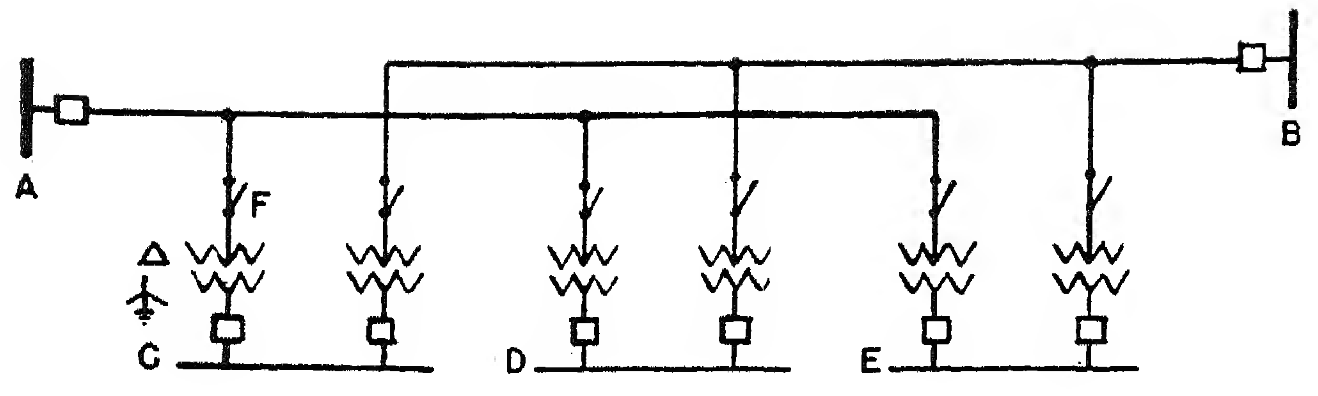


Figure 3. Radial feeders with low tension backfeeds

A, B—Prime sources
C, D, E—l-t busses or ties
F—load-break switch

channel,[5] the tone is not adapted to being pulsed as in phase comparison. The tone modulations may be in amplitude (AM), including single side band with carrier suppressed, or in frequency (FM). For example, AM may be used for voice, with FM receivers for relay carrier. The transmitter then is converted to FM when a directional comparison tone is to be transmitted. FM thus is used for relaying in order to eliminate the large d-c swing on received signals, which occurs in AM receivers.

4. *Frequency Inversion*. This name has been applied by the authors to a combination believed to be original with them. They believe it should become rather widely used. It is an arrangement for avoidance, plus slightly augmented controls. It has three features:

a. Two carrier frequencies are arranged as for normal duplex voice transmission.

b. Relay carrier is assigned to the alternate frequencies at each end.

c. Control means are provided at each end for shifting from voice to relay carrier transmission by action of primary relays. A careful scrutiny of Figure 2 will make the principle clear.

Frequency inversion can be used for either phase or directional comparison, or a combination of both. The unique feature is that if primary relays at one terminal cannot function from lack of fault current, the voice does not restrain the remote end because of the frequency difference. It is ideal for joint usage on a 2-terminal line, and for special cases on multiterminal lines.

The advantages of frequency inversion are:

a. It gives the primary relays complete control with a minimum of changeover equipment.

b. It retains straight-keyed, on-off, carrier for relaying.

c. It gives 2-way non-relaying channels with the entire audio spectrum free of relaying uses. The net result is economy of the carrier frequency spectrum.

d. It requires a minimum of carrier equipment. At present, double receivers are stock items. For the present it may be logical to use a second standard transmitter at each terminal. If the scheme seems to justify its promise, the conversion problems of a single transmitter for both relay and non-relay functions should be worked out. There is also economy of yard equipment in joint usage.

5. *Frequency Shift Relaying*. Transferred signals may be used, and they may be applied in two forms:

a. By installing independent frequency shift transmitters to share yard and tuning equipment with each other and non-relay signals. This implies that they may be in the same carrier frequency band, or in a separate band by virtue of double or separate tuning and trapping.

b. By the use of a voice transmitter to send a relay signal by shifting the basic frequency of that transmitter slightly.[2] At the remote terminal the AM voice receiver and the narrow-band relay receiver with its FM discriminator operate in parallel.

In Figure 6 both (a) and (b) are shown, together with directional comparison on a keyed (on-off) carrier, all applied to the same 3-terminal line.

In Figure 7 form (b) is applied in a proposed scheme to provide directional-comparison relaying in combination with communication. It is shown for a 2-terminal line, but by using wide shifts it may be generally adaptable to three terminals. If the communication is duplex, double tuning and trapping is applied conventionally. Limited experience indicates that it may not always be necessary, and that two voice characteristics may be spread sufficiently in one band.

In addition to the interesting relaying combinations just mentioned, there are possibilities of audio or intermediate frequency by-passes at section terminals. By such means channels may be extended over two or more line sections in tandem for voice, telemetering, load control, and so forth. In short, two frequencies do the work of three on a given section, and as a by-product, relay channels have the advantage of continuous monitoring.

Problems of Multiterminal Lines

Multiterminal lines will be classified as follows:

1. Radial feeders.
2. Tapped subtransmission lines.
3. Tapped system trunks.
4. Three-terminal system ties.

Except in the case of radial feeders, the discussion will be limited to cases of just three terminals. The same principles apply in items 2 and 3 when the line has more than one tap. Definitions and detailed discussions follow.

Radial Feeders

Figure 3 shows a typical case. All terminals except the prime sources have step-down transformers, usually of relatively high impedance because of their limited size, and are usually without high-tention (h-t) circuit breakers of fault clearing capability. The h-t voltages range from less than 12 kv to 138 kv and possibly more. They may be overhead, underground, or a combination of the two. The term "radial" does not exclude high-impedance low-tension (l-t) backfeeds.

In the absence of h-t circuit breakers, or their equivalent, transferred tripping, by carrier or otherwise, is needed to clear the transformer if it develops an internal l-t fault. This implies a transformer differential relay or the equivalent. It is assumed that the transformer may be made alive from (a) the feeder source, (b) the h-t leads by load-break switches, or (c) the l-t bus. The transformer protection must discriminate against magnetizing inrushes to avoid tripping the entire feeder.

It might be assumed that faults on the h-t line and the transformer h-t windings could be relayed quickly at the prime source without carrier. This is not true for mild turn-to-turn faults, which are a fair proportion of the total. For complete h-t protection there should be some means of removing the weak current (or, in the case of ground faults, capacitance current) backfeeds of the remaining unfaulted transformers. In some cases network-type reverse-current elements on the l-t backfeeds may suffice. If such reverse-current trips are made sensitive to levels practiced on network relay settings, network-type reclosures also will be needed. The alternative to the network principle is the use of transferred tripping from prime source to transformer l-t circuit breakers. If the terminals are unattended (including supervisory) embarrassing lock-ins of trip circuits or trip signals are to be avoided, which requires attention to detail design.

There is a distinct need, primarily on urban-type systems, for cheaper and more convenient means of remote tripping than pilot wires or conventional forms of carrier.

The expedients under items 2 and 3 in the following text can be used only in special cases.

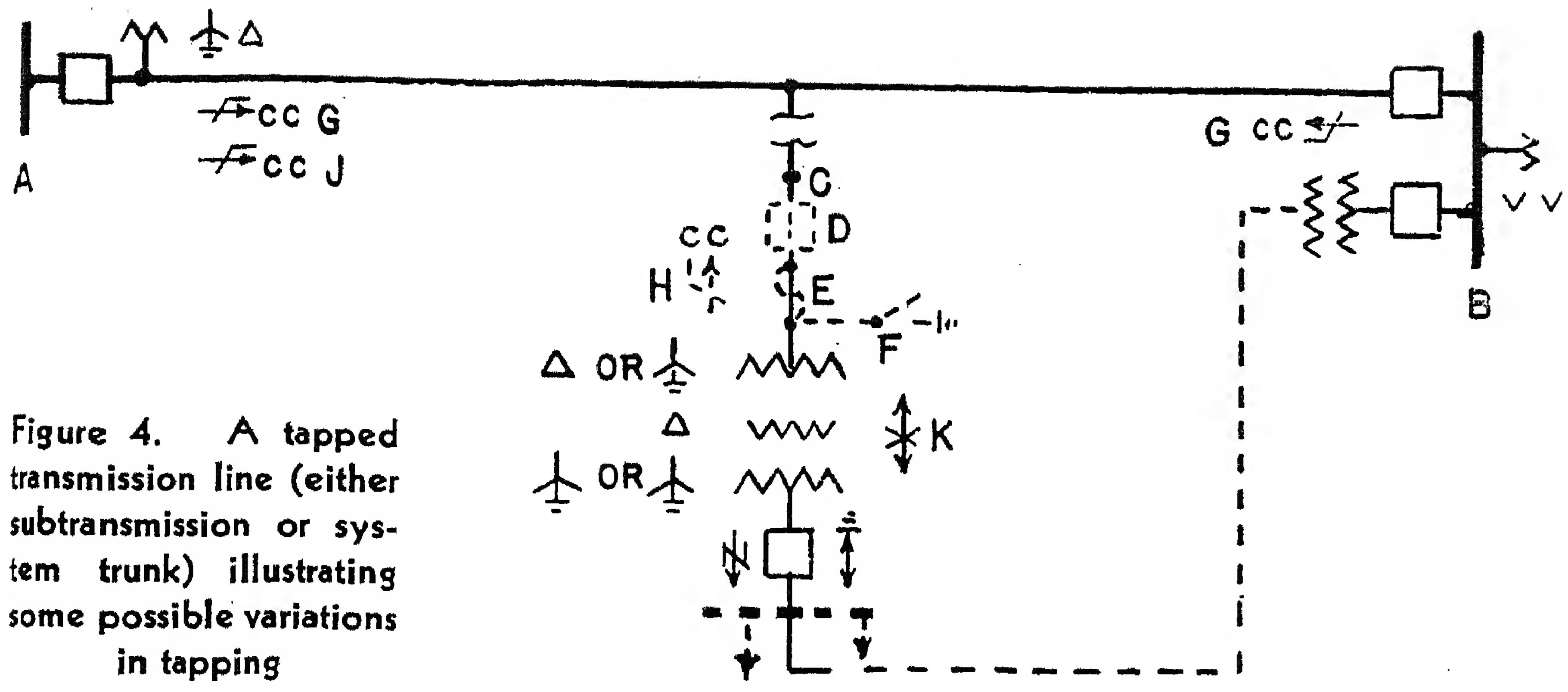


Figure 4. A tapped transmission line (either subtransmission or system trunk) illustrating some possible variations in tapping

A, B—prime terminals
C—tap terminal at an indeterminate distance from the line A, B
D—possible circuit breaker or load-break switch
E—possible fuses
F—possible automatic ground switch (1 phase or 3 phase)
G—carrier relay, phase or directional comparison
H—carrier at tap (like at A and B, or to trip A and/or B, or to block A and B)
J—carrier to trip circuit breaker D
K—possible transformer differential or an equivalent

Tapped Subtransmission Lines

This implies that there is a load take-off from a tie line through a step-down transformer. The term subtransmission means that the two main terminals normally are held in synchronism by other stiff system ties. Lines in the 69-kv voltage class are common in this group, although 34.5- and 138-kv lines sometimes may answer the description. See Figure 4. Carrier relaying at terminals *A* and *B* may have been installed before the tap was added, or such relaying may be contemplated. It is assumed to be of either the directional- or phase-comparison type.

Some problems are:

1. For economic reasons a h-t fault clearing circuit breaker may be omitted at the tap, but if the transformer develops an internal fault it must be de-energized, and eventually disconnected.

2. It is undesirable for the line to be tripped for faults beyond the l-t circuit breaker.

3. It is undesirable for the line to be tripped for faults beyond terminals *A* or *B*. Terminal *C* might cause such tripping because it may be a source of fault current of one or more of the three phase sequences.

4. It is undesirable for the line to trip on magnetizing inrushes when the line and attached transformer are made alive. The trip-out may be interpreted as an indication that the line itself is in trouble. Sequential closures of the circuit-breaker poles at *A* or *B* may contribute to the tripping effects of the inrush.

In dealing with these problems, some or all of the following expedients may be considered:

a. Re-examining and making minor adjustments of the carrier control settings, but otherwise ignoring the tap. If the transformer is of high impedance, this may be practical, except that transformer protection for l-t faults definitely is lacking.

b. Installing a set of h-t transformer fuses.

c. Applying network-type relays to the l-t side to clear the transformer (and line) from the l-t source. See the previous section on "Radial Feeders."

d. Adding a carrier set at *C* to operate from l-t current (phase comparison) or l-t current and potential (directional comparison). This assumes that adequate phase matching or polarization can be arranged. Unless the transformer is quite small, desensitizing devices (with resets) will be needed at terminals *A* and *B* to permit satisfactory closures. If the transformer at *C* can be energized locally by a h-t load-break switch, desensitizing might be done from there in the form of carrier steadily transmitted for an interval.

e. In all the previous cases the transformer protection is not very sensitive, hence a differential scheme, or an impulse-type pressure relay, may be applied to the transformer.

f. The transformer protective device may close a single-phase grounding switch to trip terminals *A* and *B*. Transformer protection which essentially ignores magnetizing inrushes is a prerequisite. If it is important to reclose manually or automatically the *A* to *B* tie fairly quickly, a load-break switch *D* then may function automatically to remove the faulted transformer and ground from the tie. The automatic opening may be from an interlocked timer[8] or from an undervoltage (l-t or h-t) relay by way of the trip circuit of the transformer protective device.

g. If fuses were installed as in (b), a 3-phase set of spring-operated grounding switches may be used to blow those fuses in case trouble is indicated by a transformer protective device. The line may be tripped at *A* and *B*, but can be reclosed immediately, perhaps automatically.

h. If the transformer at *C* has h-t current transformers, it is feasible to shift the carrier controls at *C* to the h-t side. They may be either for phase comparison or directional comparison. For the latter, they may have either h-t or compensated l-t polarization. The current transformer may be either on the outgoing or the neutral end of the h-t windings.

Note that if the tap transformer is fairly large, relatively, or has a h-t neutral ground plus a low impedance tertiary, the application of h-t carrier controls may be required to solve problems 3 and 4. Even granting these controls at *C*, the carrier trips at *A* and *B* tend to be somewhat less sensitive than on a 2-terminal line, as it is necessary to add margins between terminals to insure co-ordination in the presence of the third feed. This will appear in the settings of the tripping level fault detectors in phase-comparison schemes, and the ground current trip elements in directional comparison. Some allowance also must be made for inrush components, even with directional comparison extended to terminal *C*. At terminals *A* and *B*, line, not bus, connected potential transformers reduce the combined effects of inrush and circuit-breaker pole transitory phenomena.

i. With h-t carrier controls, the transformer protection at *C* probably would be adapted to actuate transferred tripping to terminals *A* and *B* instead of local grounding switches.

The foregoing items (a) to (i) do not include all the subterfuges that may be used in the absence of a high-tension fault clearing circuit breaker at terminal *C*. Also, such a circuit breaker actually may exist, in which case transformer clearing is completed locally. Tapped system trunks are discussed on that basis, and tapped subtransmission lines may use similar devices. A working example of relaying on a tapped subtransmission line follows:

Between the Commonwealth Edison Company and its associate, the Public Service Company of Northern Illinois, is a

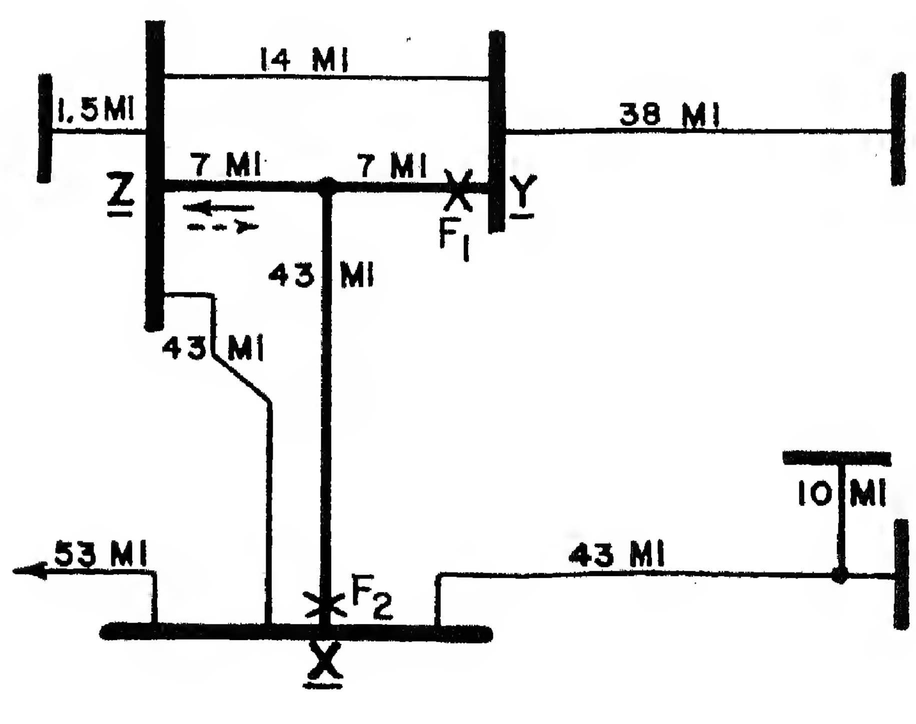


Figure 5. System layout showing a 3-terminal system tie

F_1—fault location for out-feed at Z (solid arrow)
F_2—fault location for out-feed at Z (solid), possibly followed by a weak in-feed (dotted arrow) after the circuit breaker at X opens

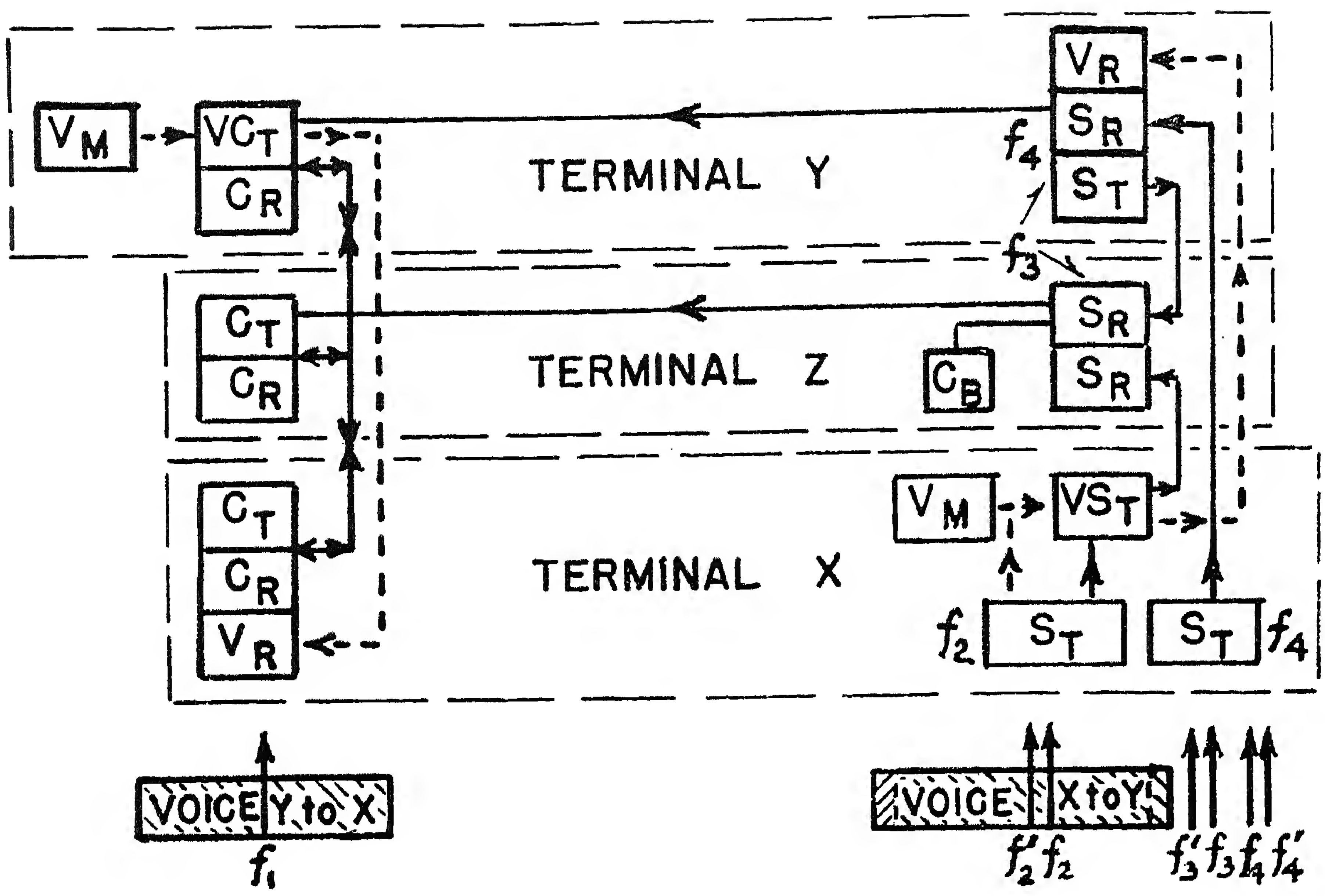


Figure 6. Relaying and voice for the 3-terminal system tie

f_1—basic frequency for directional-comparison relaying (modulated for voice)
f_2—basic frequency for voice (shifted for transferred signal)
f_3, f_3'—narrow band for transferred signal (coupled and trapped with f_2)
f_4, f_4'—see f_3, f_3'

C_T—comparison relaying transmitter
C_R—comparison relaying receiver
V_M—voice modulator
VC_T—transmitter for voice and comparison relaying
V_R—voice receiver
S_T—frequency shift transmitter
S_R—frequency shift receiver
C_B—permissive carrier trip for line circuit breaker

subtransmission line with two taps. One tap, to an industrial customer, has a h-t circuit breaker, but no carrier equipment except traps. On the other tap is one transformer of a pair in a distributing station. The h-t winding is delta-connected, with no h-t circuit breaker. This second tap corresponds to terminal *C* in Figure 4. Directional-comparison carrier is installed for terminals *A*, *B*, and *C*. It is conventional in many respects, but has the following unusual features:

At terminal *C* the transformer differential relays start carrier after a short (6-cycle) hesitation, and apply audio tone modulation for remote tripping at *A* and *B*. The tone receiver relays at *A* and *B* must be energized for 6 cycles to produce tripping. The direct, single-frequency use of the comparison carrier channel to produce remote tripping is unusual, but is workable in this installation, because non-relay usage of carrier is negligible. The hesitation at the tap transmitter gives the comparison carrier initial priority in case of line faults. To prevent an incoming tone-signal at *A* or *B* from being blocked out of the receiver by local transmission, the following controls are used: For interphase faults the third-zone elements at *A* and *B* not only start carrier but supplant the usual second-zone elements in monitoring the directional elements for carrier stoppage. The ground directional elements can stop carrier, and do so independently, unmonitored by any current element. The differential relays at *C* are not necessarily immune to inrushes, but actuate auxiliary relays which can be reset by remote supervisory control, thus stopping the carrier signal from *C* at the discretion of the supervisory operator.

Tapped System Trunks

Figure 4 also applies to this class, but circuit breaker *D* of fault clearing capability on the h-t side of the load take-off transformer is almost inevitable. The class will be further limited to overhead lines, probably 115 kv or higher. There may be system trunks below this voltage, but they are usually short, urban, and probably underground.

Several possible variations remain, as in the size (impedance) of the tap transformer, its connections, including neutral grounding on h-t and l-t, and as to whether there is a tieback from *C* to terminal *A* or *B*.

Problems 2, 3, and 4 of the preceding section must be solved for tapped system trunks also, and the following in addition:

5. The relaying must be designed for high speed to increase transient system stability and permit heavy loading.

6. High-speed automatic reclosing is to be applied for the objectives as in 5, which means that a fault arc must be extinguished promptly, even when the fault does not affect stability directly. Thus, the tap circuit breaker *D* must be opened quickly for all line faults. Otherwise the l-t feed, which may be only motor regeneration, tends to maintain an arc. The reclosures at *A* and *B* thus will fail to hold, with additional shock to the system from the re-established fault.

With a h-t circuit breaker at *C*, it is hardly necessary to provide transferred tripping from *C* to *A* and *B*, but it may be necessary to have a transferred trip from *A* or *B*, or both, to *C* to remove the tap feed promptly. This trip signal may have to pass an intervening fault location.

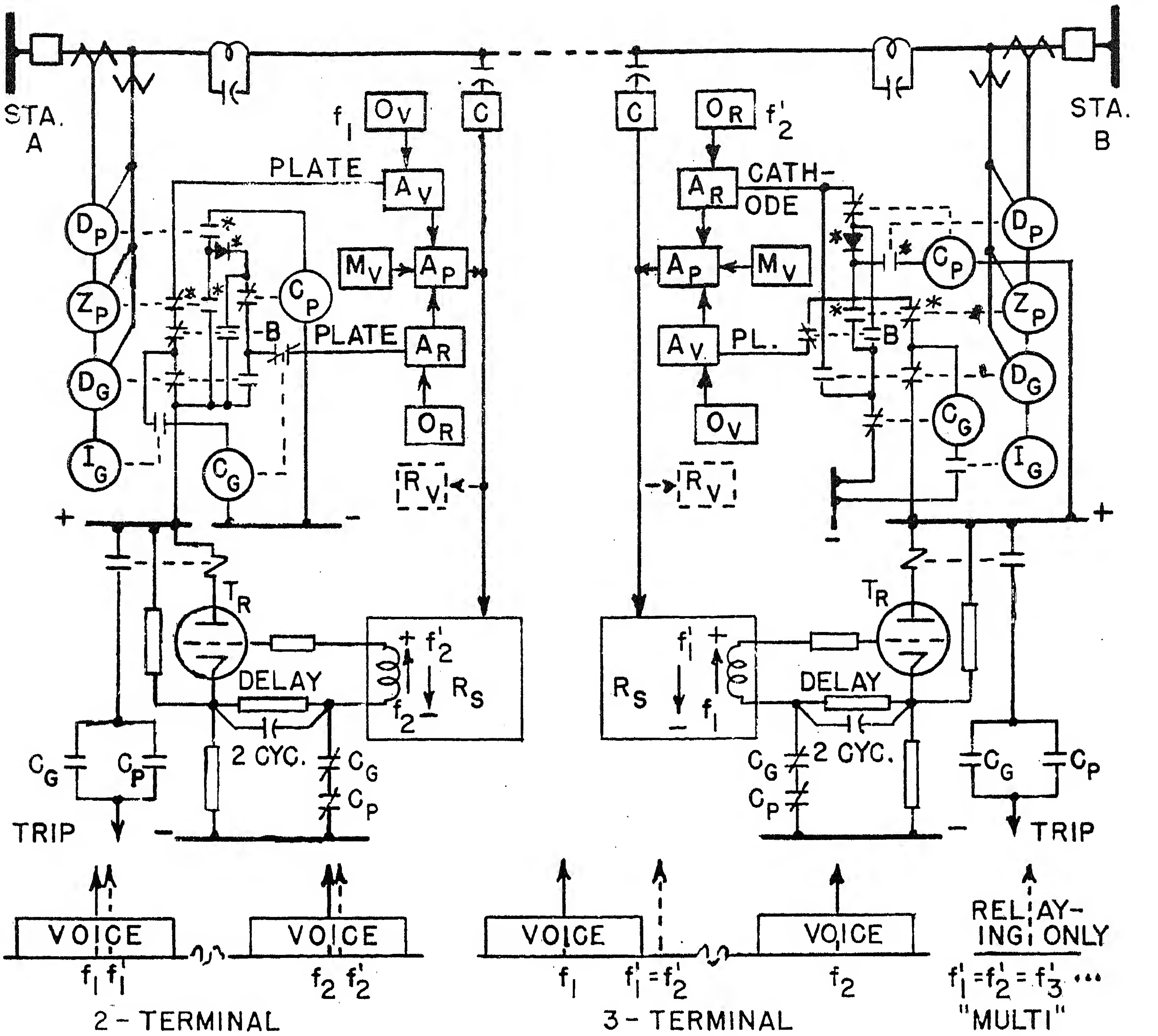
If the transformer is essentially on the right-of-way between *A* and *B*, installing transferred trips from both *A* and *B* will practically assure a clear path for one of them. If there is an extended high-tension line to the transformer location, a fault may intervene, but a limited experience in a different type of installation indicates that the signal will go through regardless.

A low-tension tie from *C* to *B* (or *A*) may cause another type of delay. For a fault on the protected line near *B*, there may be an initial outflow of fault current at *C*. This causes carrier to be sent from *C*, blocking comparison carrier relaying at *A* and *B*, until the line opens at *B*, presumably from a first-zone or instantaneous element. The three terminals thus probably open in the order *B*-*A*-*C*. This clearing sequence applies to both phase- and directional-comparison type relaying. Any element at *C* which is sensitive enough to open that terminal quickly for all line faults also will produce the initial blocking as mentioned. Transferred tripping from *B* may make *C* almost coincident with *A* in the sequence described, thus providing for safe reclosure more quickly.

The company with which the authors are associated has been confronted with the following problem: A trunk line between terminals *A* and *B*, now equipped for phase-comparison relaying with high-speed automatic reclosure, has been tapped to a transformer on the primary right-of-way. The transformer is not large, but has a delta tertiary and solidly grounded h-t and l-t neutrals. There may also be a l-t tieback to terminal *B*. This

Figure 7. Directional-comparison relaying by narrow-band (frequency shift) equipment

f_1, f_2—basic voice frequencies
f_1', f_2', f_3' . . .—relaying frequencies
D_P, D_G—directional elements: P—phase (operates on feed toward line); G—ground (operates on feed toward bus)
Z_P—phase distance; I_G—residual current (both nondirectional)
*—sets of three contacts: "b" in series, "a" and rectifiers separated by phases
B—test button relay contacts
C_P, C_G—contactors: P—phase; G—ground
O_V, O_R—oscillators: V—voice; R—relay
A_V, A_R, A_P—amplifiers: V—voice; R—relay; P—power
R_V, R_S—receivers: V—voice; S—relay
M_V—voice modulator
T_R—relay tube
C—carrier coupling
T—trap (double tuned if required)



transformer terminal C is unattended without supervisory control, and is to have time-delayed h-t and l-t reclosing. The transformer is relayed differentially with h-t and l-t circuit breakers and a set of h-t bushing potential devices. A transmitter is to be installed to provide steady signal blocking during transformer and l-t faults and during magnetizing inrushes. To provide restraint at remote terminals it is necessary to have devices which do more than ignore inrushes. Thus, when the transformer is energized from h-t or l-t or from terminals A or B, the transmitter at C is started by instantaneous voltage relays, then deactivated by a timing device.

Terminals A and B should not be tripped by transformer or l-t faults. Only close-in l-t faults draw enough current to affect the comparison relay tripping elements at A and B terminals. Hence, the fast-acting transformer differential and the instantaneous l-t phase distance and ground relays are used to start the transmitter for blocking. These relays could be limited to carrier starting, but are also l-t circuit-breaker tripping relays for the present. When more l-t lines are added, this arrangement, if unaltered, would depend on slow speed reclosures to restore service if the transformer distance and ground relays tripped on l-t line faults.

Note that there is no carrier start at C for faults on the h-t line. For h-t faults at B the l-t reach of the fast-acting carrier start elements at C is too limited. Hence, there should be no sequential carrier relaying for h-t faults.

The transferred trip is from A to C by a frequency shift channel, with a reverse channel from C to A to monitor against loss of the guard frequency or the equivalent.

To set up and release h-t and l-t circuit-breaker trip coil circuits at C in addition to all of the other functions set forth here, without sneak circuits and/or lock-ins, requires careful planning of control circuits at C. No relay action or remote signal should lock out terminal C, h-t circuit breaker D, except the action of the transformer relay. Basically, however, the problem is assumed to be solved.

Joint usage in this case is to be negligible so that one comparison frequency, plus the narrow bands for frequency shift, should be adequate. At terminal A the transferred trip transmitter (narrow-band) should not block the rather non-selective phase-comparison receiver. At C there is to be no comparison receiver.

As a hypothetical problem, suppose joint usage is intended. Frequency inversion as indicated in Figure 2 then could be applied at terminals A and B. This would provide a duplex non-relay channel between A and B, but leave the phase-comparison relays in full control. Blocking transmitters for both frequencies then would be provided at terminal C. Double-frequency tuning and trapping obviously would be needed. The authors believe that frequency inversion would be almost ideal for joint usage in this case.

Three-Terminal System Ties

In these installations there is no lumped impedance, such as a transformer, in any of the three legs, and all terminals are actually or potentially strong fault current sources. One example, of this, namely a short line with no out-feed for internal faults, presumably was solved by the arrangement of Figure 1.

A much more difficult case is being studied. The line layout is shown in Figure 5. The line, with terminals X, Y, Z, is to be a main system tie, with automatic reclosure. It has one long and two

short legs; one of the short legs (to Z) will under some conditions have out-feed (solid arrow), for faults at F_1 or F_2. Also, it would be desirable to have duplex communications between the terminals X and Y. A study of the relaying with settings co-ordinated to the short ties beyond Y and Z showed very slow clearing if no carrier is used. The minimum clearing time for second-zone faults would be about 1.2 seconds. In the rare event that the out-feed at Z would be followed by a weak feed (indicated by dotted arrow) there would be double sequential relaying, and a total clearing time of about 2.5 seconds.

Directional-comparison carrier relaying encounters the same difficulties from the out-feed at terminal Z for internal line faults near terminals X and Y. Under such conditions the carrier signal from terminal Z would not be removed until the circuit breaker at X (or Y) opened. This sets up the delay encountered on single sequential relaying. For a fault at F_2, a weak in-feed at terminal Z (dotted arrow) may follow the circuit breaker opening at station X. Theoretically, at least, the apparent distance registered on the relays at terminal Z after the circuit breaker at X opened may be as high as 160 miles. It is hardly practical to set the carrier trip (second zone) at Z for such distance, hence Z would not set up a carrier trip until the circuit breakers at both X and Y had opened. To prevent the circuit breaker at Y from being thrown back on timer operation by the sustained signal from Z, it is necessary to either (a) have the carrier stop at Z monitored by a sensitive third zone, after the manner described for a tapped subtransmission line, or (b) install transferred signals as illustrated in Figure 6. The total restoration time, that is, the time to clear an arc and re-establish a synchronous tie by reclosure under the worst conditions with third-zone control at Z was worked out to be 40 cycles. This included 25 cycles of reclosure hesitation at terminal X.

In Figure 6, the f_2-f_2' and f_3-f_3' signals would be shifted by the first-zone interphase elements and the general-purpose ground relays.

The transferred signals f_2-f_2' and f_3-f_3' could be applied in two ways. If used to trip circuit breakers directly, the restoration time could be reduced to 20 cycles, which is adequate, but such use of single transferred signals may not be safe with respect to spurious tripping. As an alternative the transferred signals might be used to remove the unwanted signals from terminal Z, so that directional comparison at X and Y would be expedited. This, plus semidirect tripping of the circuit breaker at Z, would give a total restoration time of 30 cycles, which is probably too slow. All these restoration times might be appreciably longer in the case of single line-to-ground faults.

The duplex communication between terminals X and Y calls for the use of a voice transmitter at X as shown, which keeps the comparison relaying function in the clear by frequency difference. For terminal Y there is a third frequency shift signal f_4-f_4' which is shifted by the second-zone, or ground, relays acting on the corresponding shift transmitter at terminal X. The shifted signal received at Y has the function of taking the normal voice controls away from the combination transmitter at Y. Thus, the relaying function is not hampered by a voice signal from Y, even in the absence of enough fault current at Y to take over control.

As regards the transferred signals, it is necessary to keep them spaced in the carrier spectrum so that they do not interfere with voice or comparison carrier or vice versa. There is joint usage of the voice transmitter at terminal X, in that its oscillator is supplemented by the frequency shift signal generator after the manner described by R. W. Beckwith.[1] Actually a pair of crystal controlled oscillators would be used, one for voice, the other for the shifted signal.

Figure 6 can hardly be considered a completely satisfactory solution. One must choose between problematical safety and the loss of speed, and this aside from the question of transferring signals past an intervening fault. Joint usage does not add much to the problem. The difficulty is with the basic relaying scheme, including the primary relay elements.

General

At present the company with which the authors are associated is inclined to use pilot-wire relaying on underground high-voltage lines, and is adapting the schemes to greater distances in spite of the cost of pilot cable. The alternatives are carrier over power line cable, or microwaves. Carrier over cable has not been investigated adequately, and the microwave field also offers us channels, but with no clues as to how to use them or what primary relay elements to apply. With a few minor exceptions, all of the problems of power-line carrier can be expected to recur in microwave applications. For example, it may be possible to separate voice from relaying in microwave tone channels, but continuous monitoring of the relay channel may be desired, as H. W. Lenser has indicated.[5] Thus, the problem of controlling the relay channel in the absence of fault current re-enters. By the frequency inversion arrangement control can be maintained on 2-terminal lines and at least some 3-terminal lines.

The advantages and disadvantages of conventional forms of carrier schemes were quite well summarized in a recent paper.[6] The present paper adds some less conventional forms and also combinations of two of the three basic principles. At best, however, some problems are unsolved, and new ones tend to be more difficult. If adaptations along present lines do not suffice, the problems should be attacked from entirely new angles. If there are no undiscovered principles, there may be radically different techniques in either microwave or power-line carrier relay applications.

Also a relay system is satisfactory only if it meets the technical requirements of the system, and takes the human reactions and limitations into account as well.

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Discussion

S. C. Bartlett (American Gas and Electric Service Corporation, New York, N. Y.): Although the material of this paper is largely of interest to relay engineers, we observe

certain phases having to do with carrier current and in particular the section entitled "Frequency Inversion." This is very interesting and as far as we know is a novel application. The saving in spectrum is not particularly apparent since power-line carrier telephone in general is operated on a single frequency, particularly where communication with a number of stations is required. The important feature as we see it is in the potential saving of equipment. Any reduction in total number of vacuum tubes or circuit components should tend in the direction of improving reliability. About the only shortcoming we observe is that under those conditions where heavy line currents may operate protector relays the relay frequencies would be applied for long periods of time and the carrier telephones rendered inoperative.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The authors have succeeded in consolidating in this paper a comprehensive review of most of the carrier and relay problems encountered on a transmission network together with many suggestions pointing toward improved solutions for these problems. We are most certainly in agreement with the authors' objectives of finding methods which will permit greater use of carrier within the available frequency spectrum. With this common objective in mind, we would like to supplement this paper by pointing out a few other fundamental considerations which must be kept in mind.

Phase Comparison

It is disturbing that the authors advocate continuous comparison of each phase current without discussion of the serious disadvantages of such a method, some of which follow:

1. The most obvious disadvantage is the use of three channels or tones instead of one.

2. It is apparent that both relay costs and channel costs would be increased in varying degrees, depending upon the method used.

3. Load current and, on the longer and higher voltage circuits, line-charging current contribute to the application limitations of phase-comparison relaying. With the American methods using a single derived value for comparison, these limitations need apply only to balanced 3-phase conditions, since negative- and zero-phase sequence currents are substantially unaffected by load and charging currents. On the other hand, comparison of currents in each conductor introduces these limitations for all faults.

(a) Load Current. With comparison of currents in each conductor, proper operation on an internal fault requires that the fault current be considerably greater than load current. During single-phase-to-ground faults, it is not uncommon for the load current in the faulted phase to be greater than the fault current.

(b) Charging Current. Since charging current can enter a sound line at both terminals, it appears to be the same as fault current to phase-comparison relaying. Comparison must not occur when charging current is appreciable relative to load current or relative to load and through-fault currents. This not only limits the possible use of continuous comparison but also limits the possible sensitivity on all types of faults when currents are compared in each conductor.

Directional Comparison

The authors enumerate two disadvantages of directional-comparison relaying. Actually, they appear to be enumerating two disadvantages of backup relaying, perhaps the very disadvantages that justified carrier relaying in the first place. For example, it is not necessary that compromises be made tending toward greater load sensitivity on backup elements. Also, although "backup elements trip without warning," they are backup elements and their tripping without warning is in no way occasioned by carrier control. The same backup elements would react the same if phase-comparison carrier were used.

The authors state that "zero-phase-sequence induction can be offset by potential polarization." Although occasionally this may be true, in most instances potential polarization offers no advantage since zero-sequence potential is simply the zero-sequence current supplied by the local transformer times the impedance of the local transformer. Usually, if the neutral current results in incorrect polarization, so also will the zero-sequence voltage.

Assuming no economic advantage one way or the other, the authors advocate the use of line potential, rather than bus potential, to polarize ground relays. Although a manufacturer might be expected to applaud such a decision, there does not appear to be sufficient justification from an engineering standpoint. As an example, when the first circuit-breaker pole closes, more zero-sequence potential will be applied to a ground relay with line-potential supply than with bus-potential supply.

The authors seem to indicate that directional-comparison relaying with distance relays is not applicable to very short lines. Again, they seem to be confusing carrier relaying with backup or duplicate relaying. There is absolutely no limitation to the short-line protection of directional comparison carrier relaying with distance relays. The limitations is in the backup relaying. Occasionally, the first-zone distance protection cannot be used, or perhaps even the second-zone distance protection, but no line is too short for directional-comparison protection.

Joint Usage

It does not follow necessarily that the crowding of the carrier frequency spectrum inevitably will force joint use of the carrier equipment for relaying and other functions. While joint use may appear to be a logical solution to the frequency-congestion problem, the present trend is away from joint use rather than toward it.

The fact that a relay channel requires a high signal level and an insensitive receiver to overcome noise, and requires line traps, makes it possible to use the same frequency for relaying on several line sections. Thus, better utilization of the frequency spectrum can be obtained than with 2-frequency operation on every line section.

Although 2-frequency operation of a carrier-telephone channel provides a very desirable type of service, it is doubtful that a system-wide dispatching telephone circuit can justify the luxury of 2-frequency operation in locations where frequency spacing is a problem.

Carrier Over Cable

The authors state that carrier over power cables has not been investigated adequately. However, there are quite a few installations of carrier over power cables and the limitations and possibilities of the various types of cable construction have been demonstrated by these installations and field tests.

G. W. Hampe and **B. Wade Storer:** With one exception, the differences between Mr. Kennedy and the authors are only in emphasis. The authors were not directly advocating any specific form of relaying. The paper includes solutions and attempted solutions of certain problems.

Obviously present types of power-line carriers are not well adapted to single-wire phase comparisons. With or without single-wire comparison, it is possible, and more or less practical, to get relay sensitivities below load currents. Getting below line-charging current is a more difficult matter.

In regard to backup elements, combining functions for carrier control does modify settings in general. In particular, on multi-terminal lines, it may be necessary to avoid multiple usage of elements and apply separate distance elements to set up carrier start, carrier stop, and carrier trip. Thus the non-carrier elements are a simpler separate problem.

As to the advantages of potential polarization, it seems that Mr. Kennedy is unfortunately right and that trippings of directional-comparison relays on spurious ground-fault currents cannot be avoided entirely, either by the use of line or bus potentials.

If a line trips when circuit breakers close poles unequally, that is a nuisance, but operators are sufficiently accustomed to such things to cope with the situation in general. It is more than a nuisance if there is a transformer tied solidly to the line or tapped to it at an unattended terminal. At least four possibilities exist:

1. Unequal closure of circuit breaker poles.

2. A sustained line fault.

3. A transformer failure.

4. A transformer magnetizing inrush (alone or in combination with item 1).

A scheme which would not operate except for item 2 would be desirable.

As to spurious currents from zero-sequence induction, that requires vigilance on the part of the relay engineer and is sometimes a real handicap in making sensitive settings and in working out ice-melting schemes. Inductions occur between lines at the same or different voltage levels if coupled on the same towers or right of way or by transformer windings. In some cases neutral currents may be combined to yield correct polarization, but in other cases such combinations may not be physically possible or practical.

In working out ice prevention or melting schemes it is disturbing to find that the line rearrangement puts the system into a setup for spurious induction tripping at a time when faults are frequent. In extreme cases lines to be melted must have phases tied

by ungrounded jumpers to prevent a fault during melting from tripping load carrying lines.

For the foregoing it would seem that, where applicable, the combination scheme which does the ground relaying by phase comparison should be used. Where this is not applicable, should the ground relays be polarized by negative-sequence potential? It would seem to eliminate that part of the problem due to induction from fault currents. Other spurious currents would still be troublesome. This should be an impetus to the development of new schemes.

As to distance relays on very short lines, the residue, after making eliminations as Mr. Kennedy did, probably would be less effective than the scheme of Figure 1 of the paper, which was specifically planned of a short line.

As to carrier over cable, the writers know of no case where a line consisting entirely of cable is carrier-protected. The alternative schemes, such as pilot wires, do not have an obvious monopoly at higher voltages and longer distances. In particular, radial feeders, as in Figure 3 of the paper, could use the all-terminal coverage of carrier to advantage, even if wholly or partly in cable.

As to whether joint usage (combining relaying and nonrelaying functions) is increasing or decreasing depends on the definition of joint usage. The gradations were discussed in Part I of this Series.[1] For direct utilization of the relay channel, frequency inversion (patent applied for) is the simplest joint usage scheme known which gives complete relay control. There seem to be few places where it fits into the system pattern, but where such places exist it should do an excellent job with standard stock equipment.

In reply to Mr. Bartlett, there are places where duplex transmission is necessary, or definitely advantageous, and there are places where it is used. Among the advantages of duplex transmission are:

1. If the channel is to be extended into a wire line, a duplex channel is better and simpler, if not necessary. The same applies if the channel is to have a local terminal, and also is extended through carrier links.

2. Continuous telemetering or load control signals are often needed in both directions, hence their transmission must be duplex. Frequency inversion is not the only way to transmit such signals, but if the audio channel is duplex and otherwise uncluttered, why not use a portion of it? It is certainly a relief to get by with two instead of three carrier frequencies over a given line section.

That heavy line currents may interrupt nonrelay usages was recognized in the paper. Suggestions from several sources as to means of avoiding or shortening such interruptions were indicated.

In conclusion, the writers would like to point out the following as particularly worthy of application:

1. Multiratio current transformers with selector switches or changeover arrangements for deicing conditions.

2. On phase comparison, interpulse metering of the remote carrier signal.[2]

3. Frequency inversion for joint usage.

The other special solutions show what can be done in specific cases, and as regards the unsolved problems, possibly someone else can carry them to finality.

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Synchronous Machines with Rotating Permanent-Magnet Fields

Part I. Characteristics and Mechanical Construction

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MEMBER AIEE

Development of powerful permanent-magnet materials, especially Alnico V, has raised generators using permanent-magnet fields from the magneto and fractional kilowatt class up to units rated at many kilowatts. The illustrations show such generators in capacities from 0.1 kw at 12,000 rpm to 75 kva at 1,714 rpm and even greater capacities are possible. The specific feature of the permanent-magnet generator is that its field has no windings and thus requires no external excitation (no exciter), no slip rings, commutator, brushes, and so forth. It has no arcing contacts to cause radio interference and is inherently as explosion-proof as a squirrel-cage induction motor.

The armature of a permanent-magnet generator may be of any conventional type and winding, but the field structure is entirely different since the necessary magnetic energy is obtained from blocks of permanent-magnet material in the field structure instead of from coils of magnet wire energized from some external source such as an exciter. Details of the field designs may be changed as required for various generator characteristics and the illustrations show a number of especially advantageous designs and methods of construction. This discussion is limited to rotating field machines since rotating armature generators would require slip rings to bring out the generated power and thus nullify one of the main features of the permanent-magnet generator. It is limited also to rotors with laminated steel pole shoes and rectangular blocks of permanent magnet material to obtain minimum rotor losses with maximum linear magnetization and simplicity for mass production.

History of Present Development

When the Alnico magnets first became available on a commercial basis, it was customary to make the smaller pole structures of a single shaped casting and larger units with blocks of magnet material bolted to the central hub or shaft. Most magnetos and some medium-capacity generators still use that simple construction with no damper bars or laminated pole shoes.[1,2] The series of developments which produced the illustrated generators was initiated by the Signal Corps Development Laboratory in 1944, after some 3 years in their laboratory, and continued by production and development contracts from various branches of the Armed Forces and also many nonmilitary contracts. The 75-kva rotor shown in Figures 1 and 2 was manufactured on a contract with the U. S. Engineer Research and Development Laboratory. This program was undertaken in three steps:

1. To discover the general configuration to obtain reasonably efficient use of the magnetic material.

2. To develop a rotor structure which would be sound mechanically, efficient magnetically, and simple to manufacture.

3. To refine the design formulas.

Paper 52-224, recommended by the AIEE Rotating Machinery Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Minneapolis, Minn., June 23–27, 1952. Manuscript submitted April 1, 1952; made available for printing May 19, 1952.

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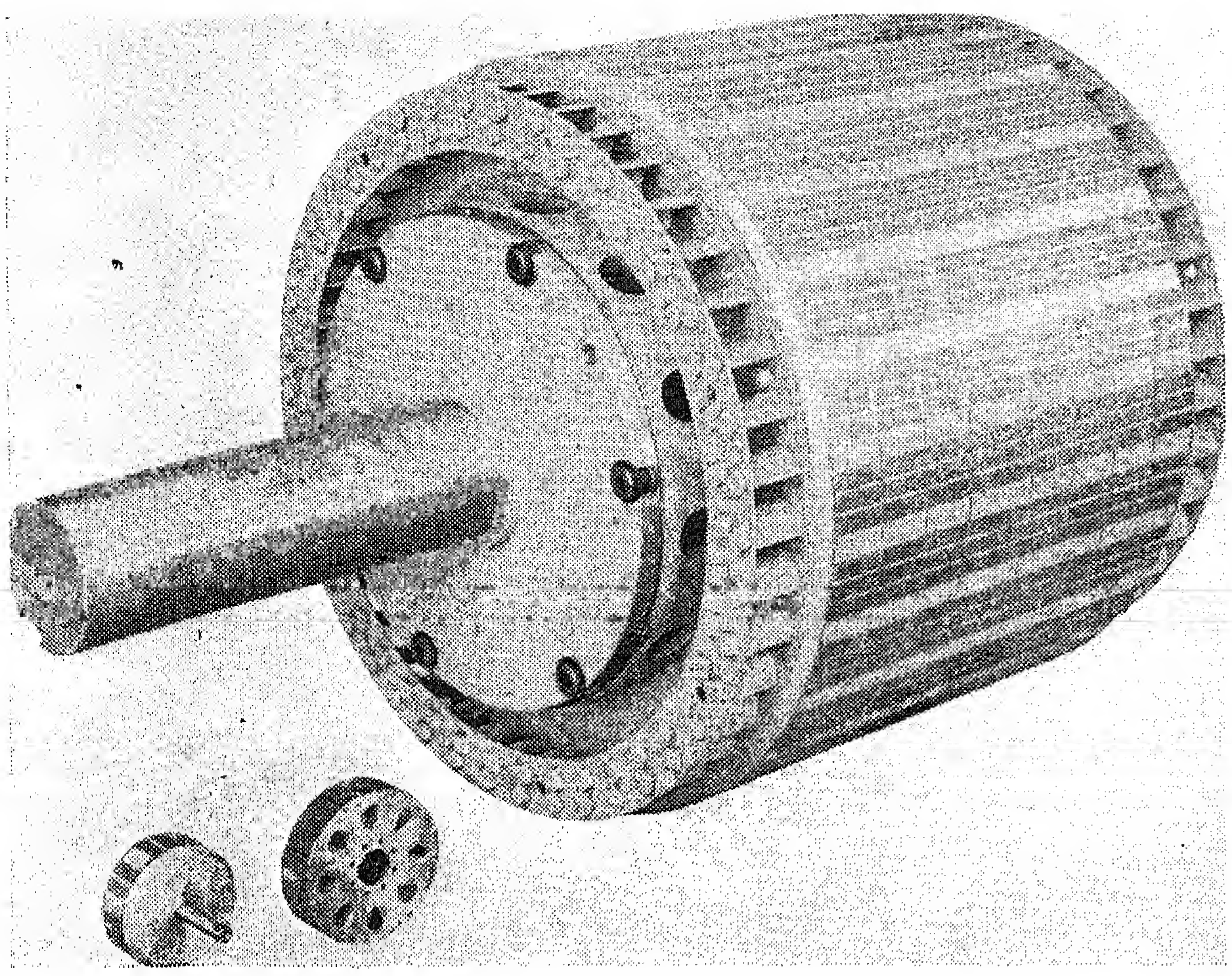


Figure 1. A 75-kva 28-pole 1,714-rpm 400-cycle rotor with drive shaft for use as a 2-bearing generator. The small rotor is for a 0.1-kw 8-pole 12,000-rpm 800-cycle generator and the uncast magnet assembly for a 0.1-kw 8-pole 6,000-rpm 400-cycle unit

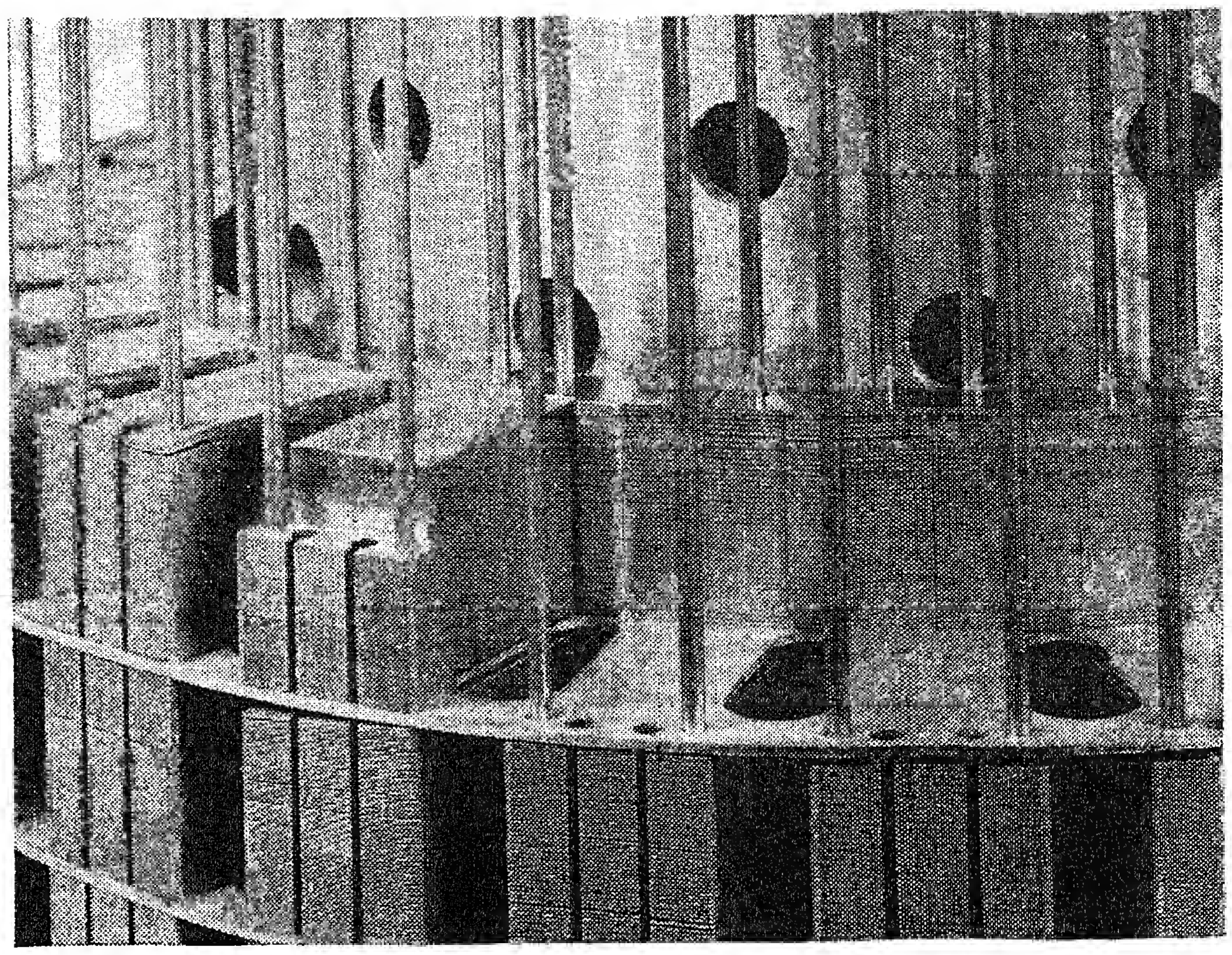


Figure 3. 75-kva 28-pole 400-cycle rotor magnet assembly, partially stacked, showing main drive tube with casting openings, inner magnet ties with loose fit on rivets, magnets in place, and pole shoes tight on the rivets. The nonmagnetic steel retaining disks hold all parts against centrifugal force, see Figure 4

The three steps were partially concurrent and the last is still in progress as described in a companion paper by Fritz Strauss[3].

The first step required many calculations and tests of units with different pole-shoe designs and various ratios of magnet length versus area. The second step started with solid pole tips bolted to an aluminum body, but centrifugal force gradually loosened the bolts. An improvement was made by using nonmagnetic bolts extending into the steel hub, but this was difficult and expensive to make although adequate mechanically. A great improvement was made by changing to laminated pole shoes mounted in a nonmagnetic steel cage and using copper rivets as damper bars. This cage was shrunk over the central hub and thus maintained a better magnetic circuit than the previous designs, but the rotor was only fair mechanically and still quite difficult to manufacture.

New Designs

The latest designs, as shown in the various illustrations, have laminated pole shoes and inner magnet ties locating the rectangular blocks of Alnico V in exact alignment between annular disks of nonmagnetic steel, all cast centrifugally with an aluminum alloy.[4] In addition to the usual advantages of obtaining a dense and true casting, the use of centrifugal casting allows the parts to be made with adequate clearance for easy assembly yet have no clearance after casting. This result is obtained by loosely riveting the inner magnet ties so that both the magnets and the inner magnet ties are free to move radially outward under the centrifugal force of the casting machine and thus maintain a tight internal magnetic circuit during casting. See Figures 3 to 5 for assembly details. The 0.1-kw rotors illustrated in Figure 1 are too small to cast centrifugally and other means are used to obtain the desired tight assembly.

In centrifugal casting the magnet assembly is secured in a special type of cast-iron mold and preheated to a specified

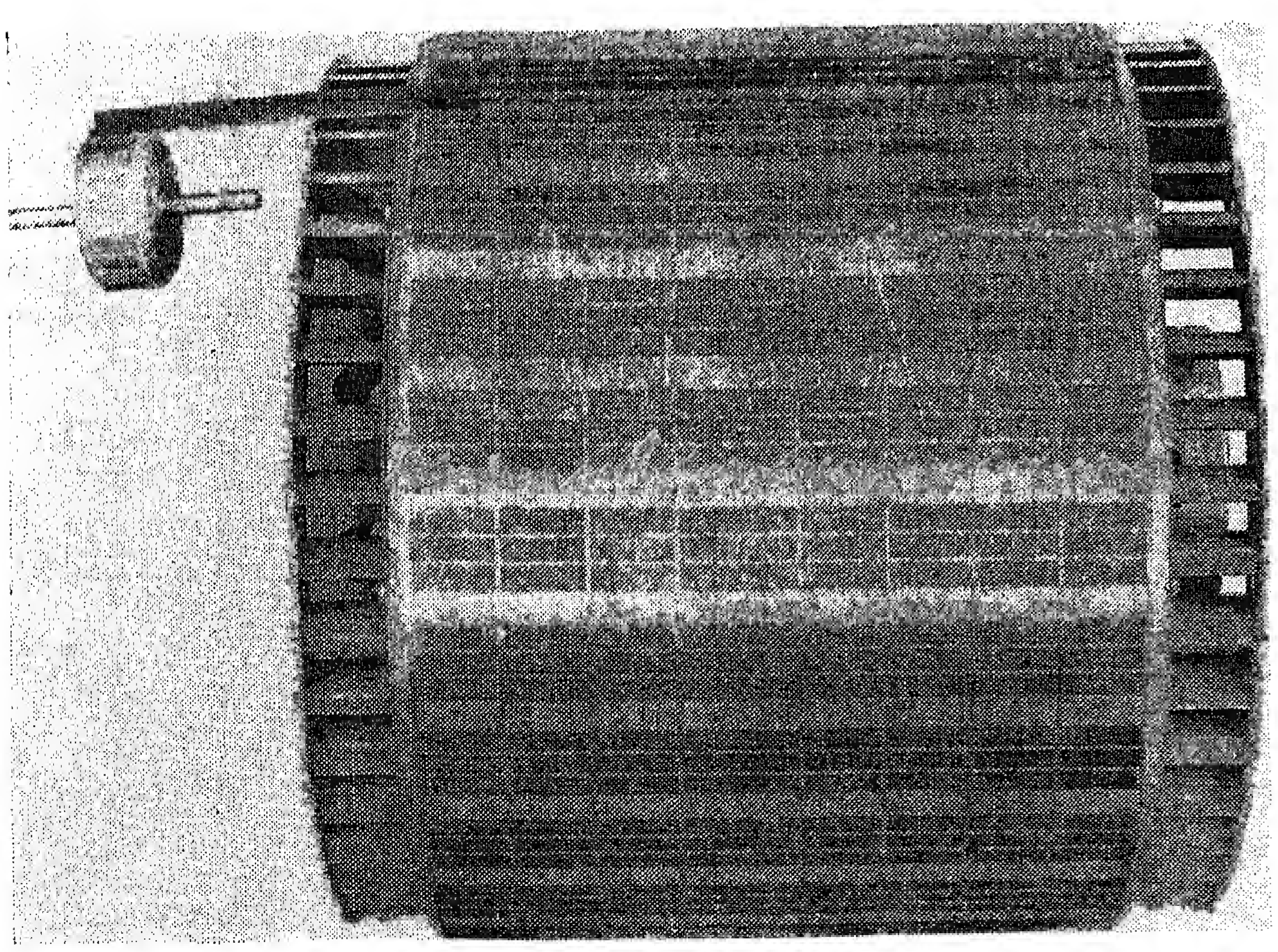


Figure 2. A 75-kva 28-pole 1,714-rpm 400-cycle rotor, single-bearing construction arranged for direct connection to the flywheel of a diesel engine. The 0.1-kw 12,000-rpm rotor is shown suspended by magnetism alone from a 6-inch steel scale. These are the same rotors shown in Figure 1

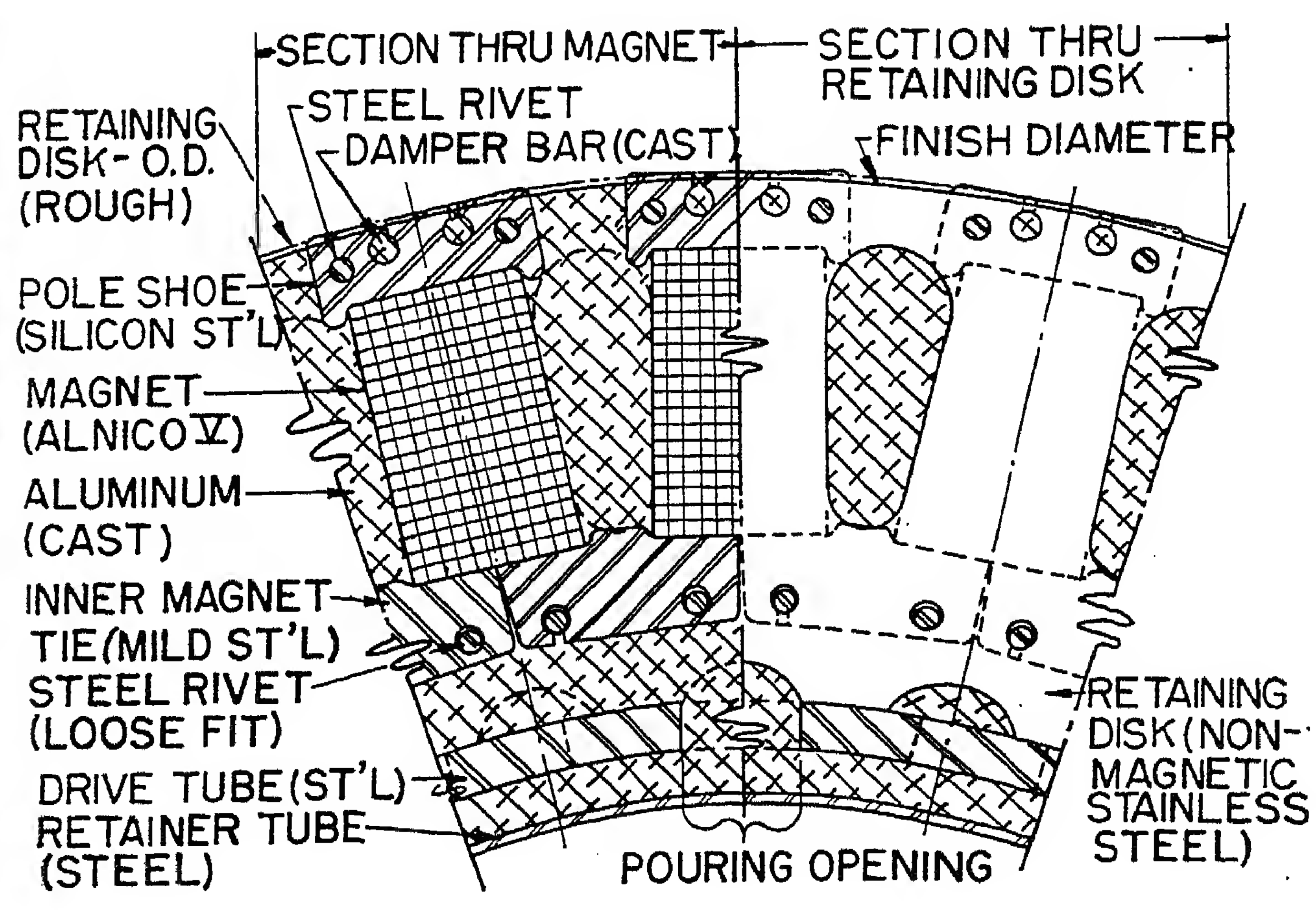
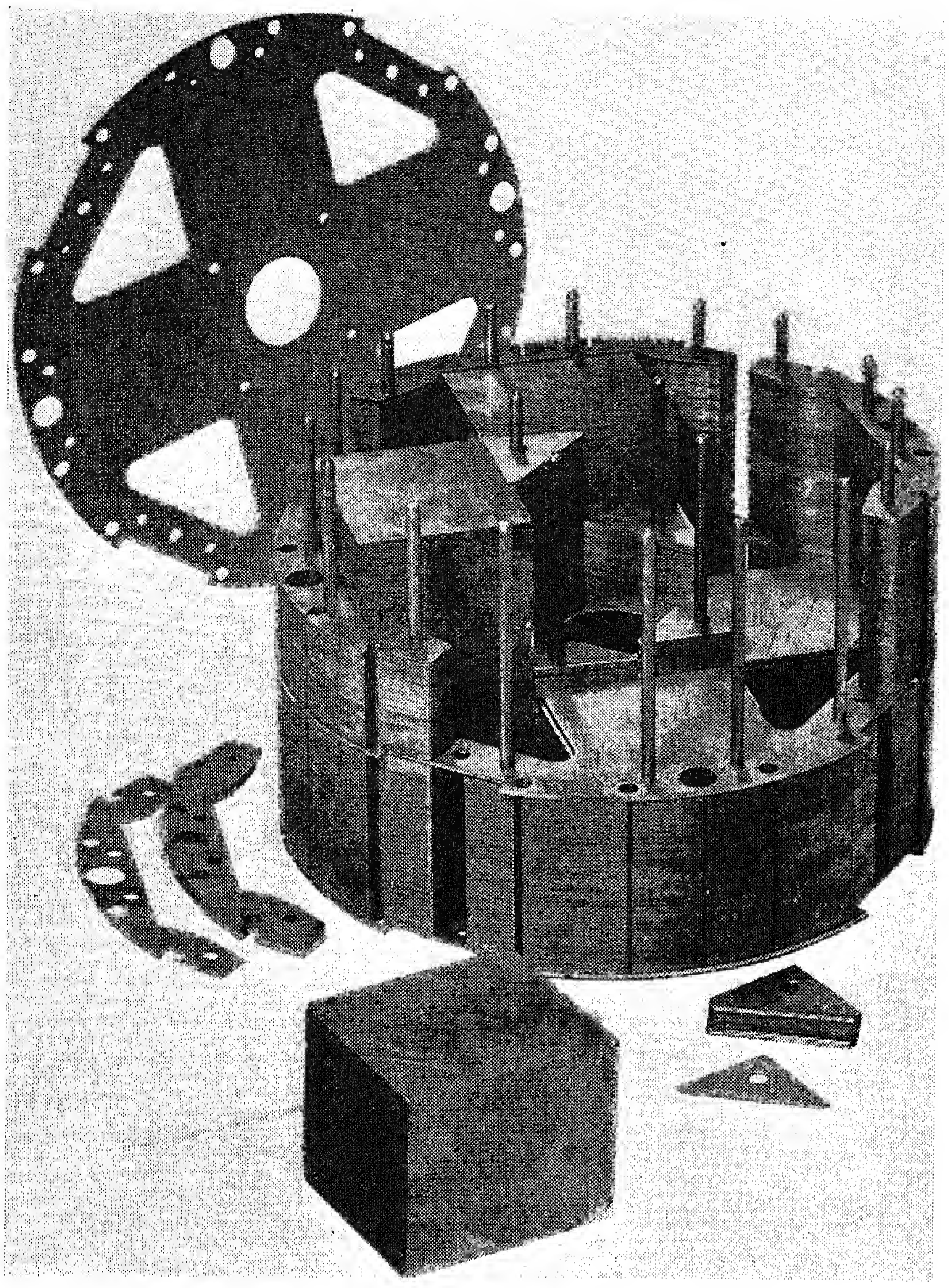


Figure 4. 75-kva 28-pole 400-cycle rotor section showing details of the magnet assembly and drive tube illustrated in Figure 3



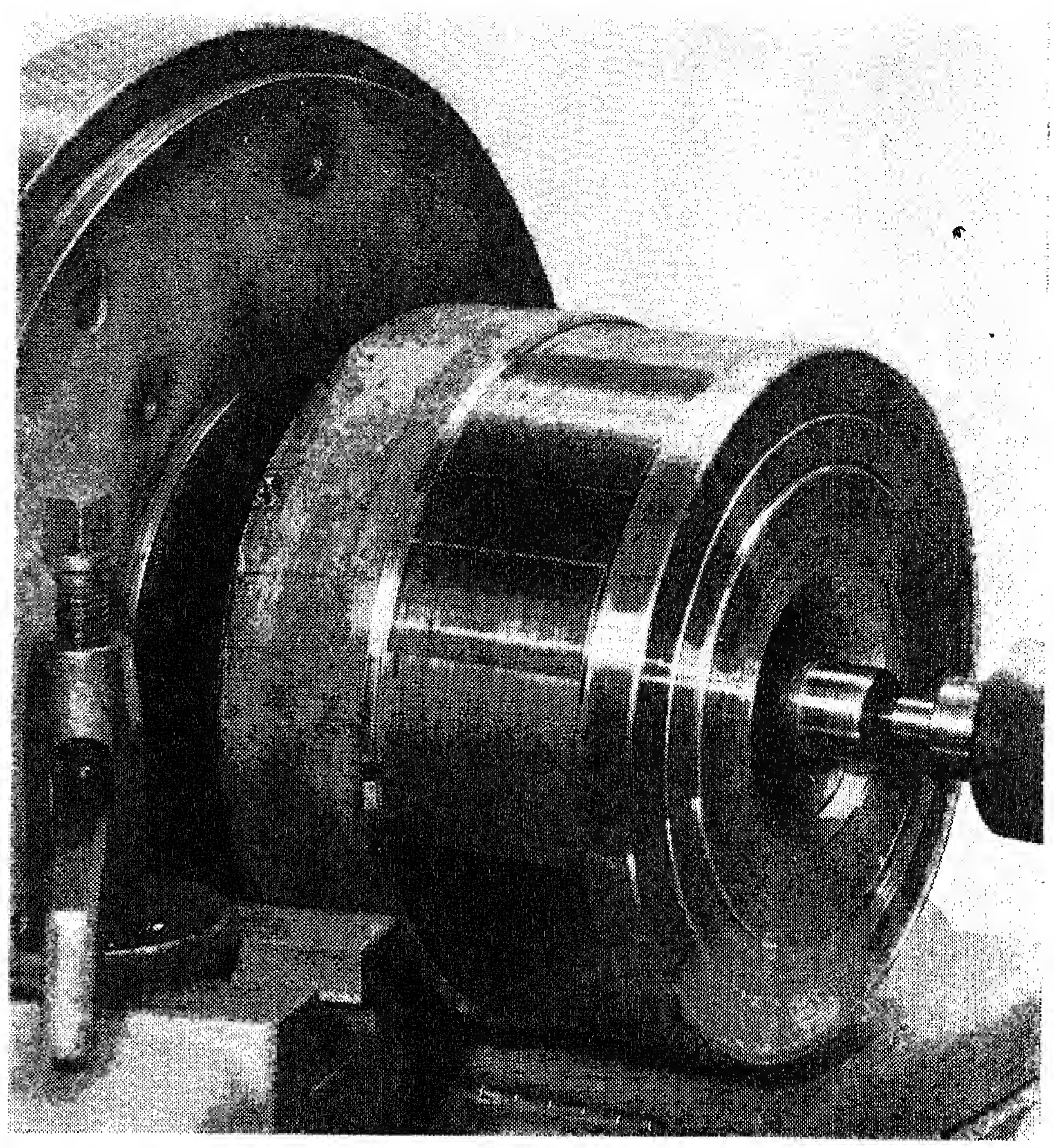


Figure 5 (left). 6.3-kva 4-pole 60-cycle rotor magnet assembly, partially stacked, showing special high-leakage extension to the pole tips and the loosely supported inner magnet ties. The main shaft is installed in this assembly after casting, see Figure 6

Figure 6 (above). 6.3-kva 4-pole 60-cycle rotor assembly cast, mounted on a lathe and partially turned to specified diameter. This is the same rotor shown partially stacked in Figure 5

temperature. The hot mold is mounted on a vertical axis and rotated at a high speed while the molten aluminum is poured slowly into the central opening. Centrifugal force fills the mold from the outside toward the center under increasing pressure with no possibility of blow holes if the mold is designed correctly. The magnet assembly may be cast as a unit with its drive hub, using the aluminum to integrate the two parts. This method is shown in Figure 3 where the aluminum will pour through the round holes in the drive tube and in Figure 9 where the aluminum can be seen inside the drive hub. An alternate method is to cast the magnet assembly alone and later shrink it onto a drive tube or shaft assembly and secure it in place by welding or other means. The rotor in Figures 5 and 6 was made in this manner. The cooling fins may be cast integral with the rotor body as shown in Figures 9 and 11.

Figure 4 shows more details of the 75-kva 28-pole magnet assembly cast on the perforated drive tube. Note the assembly clearance allowed around the inner magnet tie rivets and the slot to one of the rivet holes. This slot receives a punch press guide which stacks the ties for rapid assembly. The damper bar openings in the pole shoes serve the same purpose. The aluminum cast in these openings acts also as additional rivets in strengthening the rotor structure against centrifugal forces. In all designs the nonmagnetic stainless steel retainer disks restrain all centrifugal forces and the brittle magnets are not stressed in either tension or shear.

The pole shoes of the 4-pole rotor shown in Figure 5 have unusual contours to provide a definite magnetic leakage and good mechanical stability. The sharp break and slight hook at the start of the pole-tip elongation gives a definite width to the actual pole shoe and effectively anchors the aluminum to the steel. Permanent-magnet generators usually require a much smaller air gap than conventional units and the rotors must be lathe turned after casting, as shown in Figure 6, to remove the excess aluminum and finish to a definite diameter. All designs shown have concentric air gaps since a gentle taper instead of the sharp break and hook would leave an unsupported feather edge of aluminum over the sides of the pole shoes.

Comparison of Permanent-Magnet versus Conventional Generators

For any given application the choice between permanent-magnet or conventional electromagnet construction depends upon the relative importance given to the various specific advantages of each type. A brief summary follows.

Size and Weight

Permanent-magnet generators inherently are smaller and lighter due mainly to the elimination of the exciter and slip rings. This is especially marked in very small high-speed units and in high-frequency multipole designs where the diam-

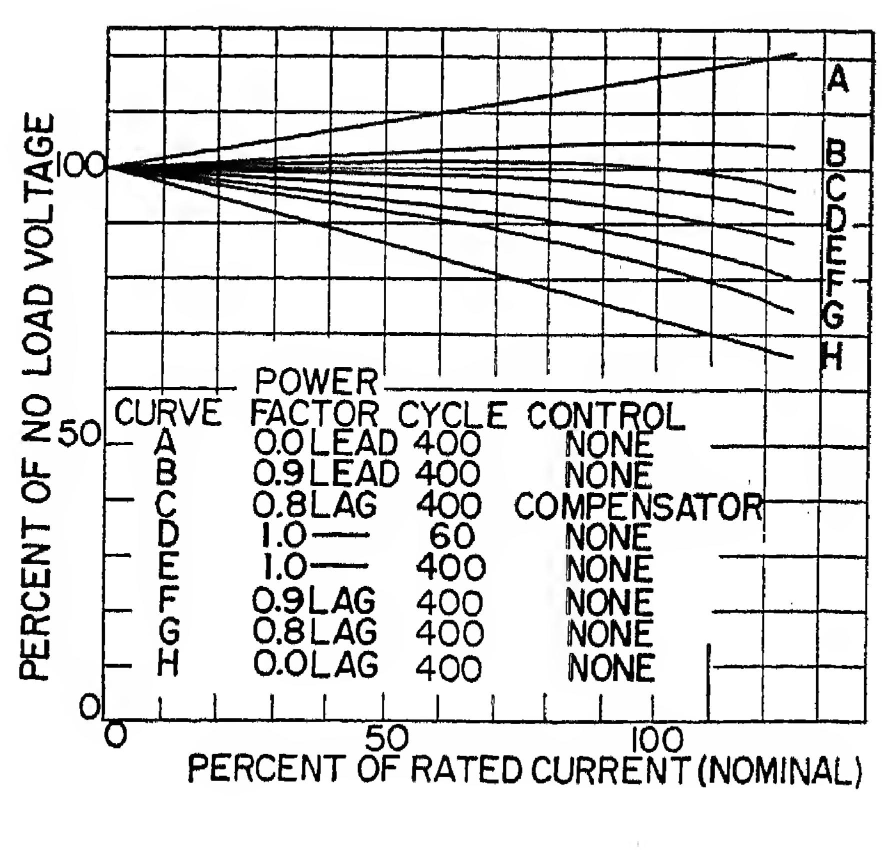


Figure 7. Voltage versus load characteristic curves of typical permanent-magnet generators with various power-factor loads both with and without a compensator

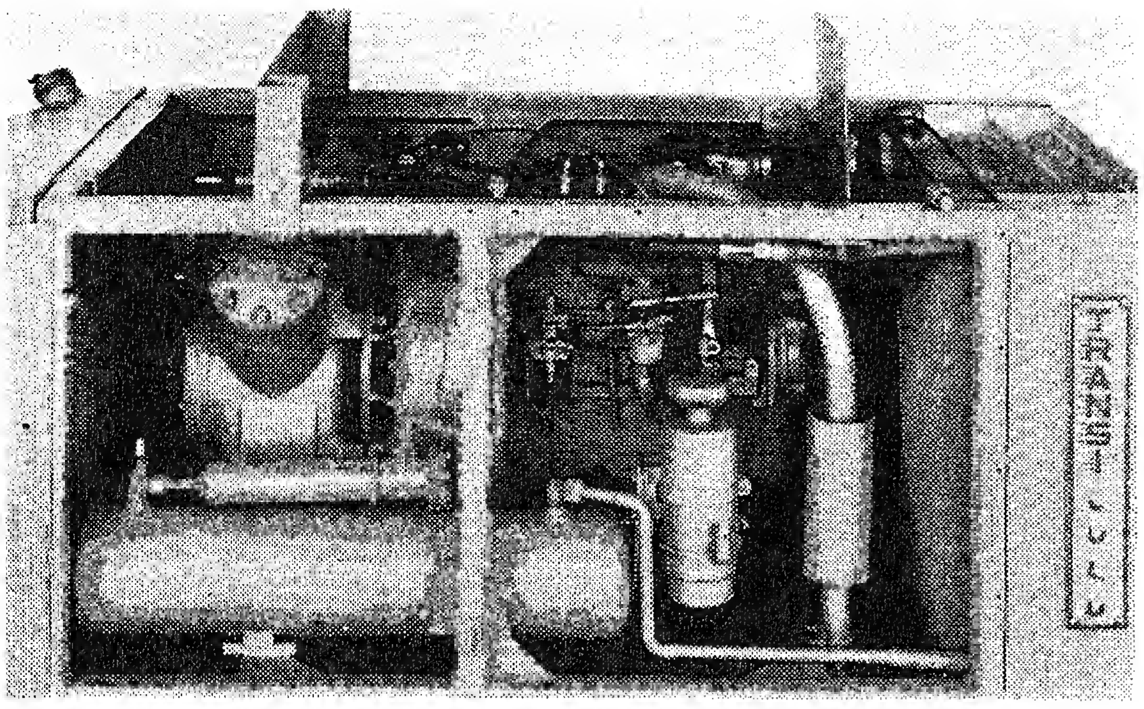


Figure 8. 1.25-kva 6-pole 90-cycle generator coupled between the gasoline engine and refrigeration compressor of a truck refrigeration unit which mounts under the trailer. The generator supplies 3-phase power for the evaporator blowers in the trailer as well as all control and battery charging

eter is large compared to the axial length. For many engine-generator applications this permits use of a no-bearing design since the rotor simply replaces the engine flywheel.

COST

The permanent-magnet rotor is inherently expensive due to the high price of the Alnico V magnets used; although the laminated structure, using punch press instead of machined parts, has greatly reduced the over-all cost. In these designs Alnico is used only as magnet material and not as any connecting or structural part.

Elimination of the exciter brings the cost of the smaller permanent-magnet generators well below that of a conventional unit complete with exciter.

EFFICIENCY

Operating cost of a permanent-magnet generator is less than for an equivalent conventional unit due to the higher over-all efficiency resulting from the elimination of all excitation losses. These eliminated losses include not only the actual field power but also the entire exciter losses and the commutator and slip-ring friction losses. Efficiencies range from 75 per cent for the 0.1-kw 12,000-rpm generator with high windage and friction losses, up to approximately 93 per cent for the 1,800-rpm units rated at 10 kw or larger.

MAINTENANCE

Elimination of field windings and the associated slip rings, commutator, brushes and so forth, eliminates practically all upkeep expense since the only remaining wearing parts are the bearings, and when practical, heavy-duty sealed-for-life bearings may be used. The rotor itself is a solid mass of metal with no insulation material and thus it is practically indestructible. Alnico V magnetization is not affected by time nor by any normal vibration or heat.

RATING—HEATING

Correctly designed permanent-magnet generators have almost no heat generated in the rotor and usually have less than conventional stator losses. For this reason, the maximum capacity of a given unit usually is determined by inherent voltage regulation limitations rather than by heating. The very small high-speed units may be operated completely sealed without exceeding class *A* temperatures, but larger generators require reasonable cooling and when operated with compensators, or on high power-factor loads, heating may become the limiting factor.

Voltage Regulation and Control

The inherent voltage regulation of a permanent-magnet generator is somewhat similar to that of a highly saturated electromagnet machine (with full saturation maintained at all speeds) and thus varies from fairly close regulation for unity power-factor loads to poor regulation for highly lagging power-factor loads. See Figure 7 for typical regulation curves based on a nominal current rating which may be varied over fairly wide limits depending upon the generator application and the associated control equipment. Leading power-factor loads tend to increase the voltage so that close voltage regulation can be obtained if each load is power-factor corrected (or slightly over corrected) by adding sufficient parallel capacitance. Where it is not feasible to correct the load power factor, a series capacitor device called a compensator may be used to increase the voltage for lagging power-factor loads, but it cannot correct for the voltage drop on unity power-factor loads.

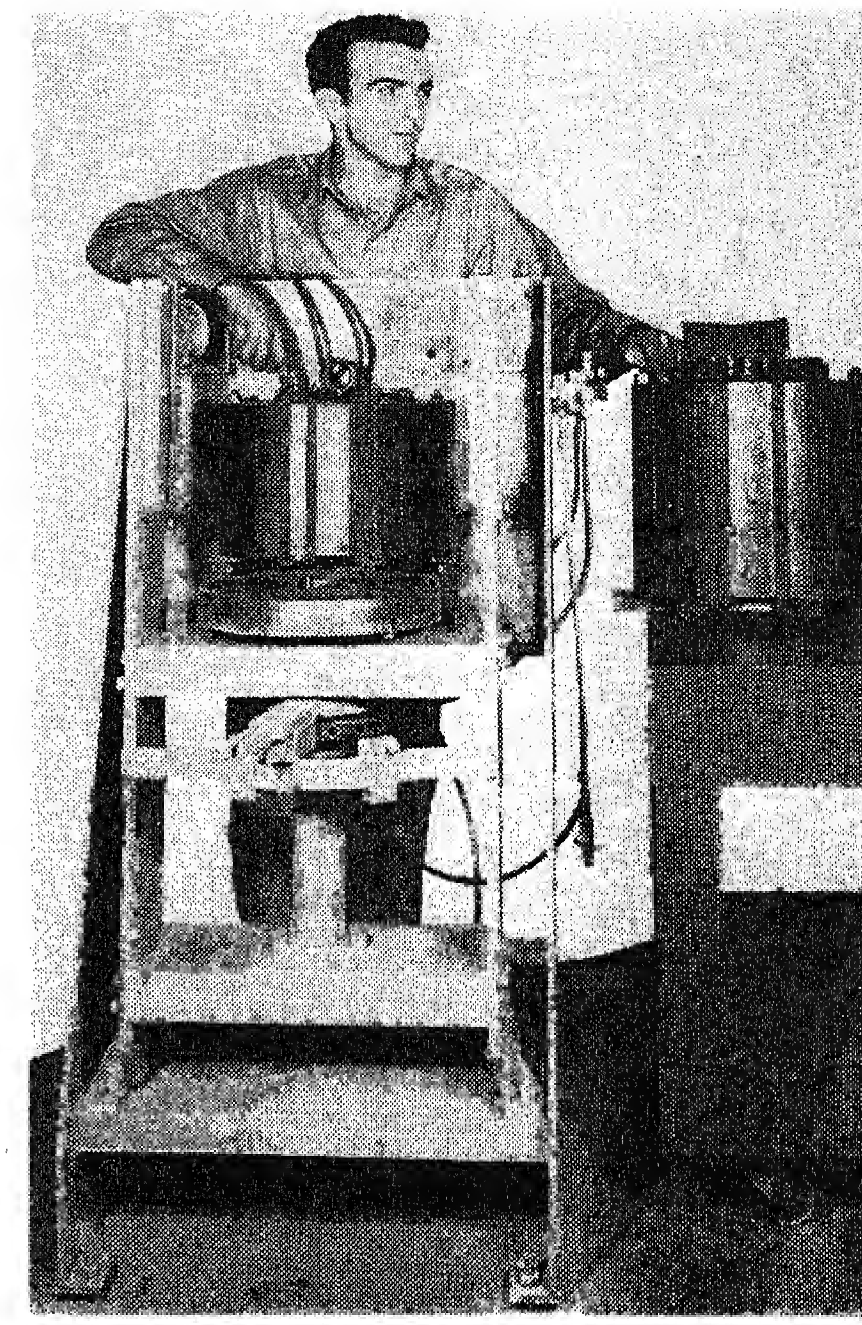


Figure 10. Magnetizer for large 14- and 28-pole rotors, 75-kva 28-pole 400-cycle rotor, maximum capacity. Twelve-inch diameter, 5- to 20-kva 28-pole charging plate in machine with 15¼-inch 20- to 75-kva 28-pole plate unmounted

COMPENSATOR

A compensator for low-voltage generators (under 500 volts) consists of a series transformer in each phase lead with a capacitor connected across each secondary. On a generator rated at 120/208 volts, the transformer would have a ratio of approximately 10 to 1 to work the capacitors at an economical maximum voltage of 300 to 400 volts for a corrective action of a 30 to 40 volts at maximum zero power-factor load. By interconnection between

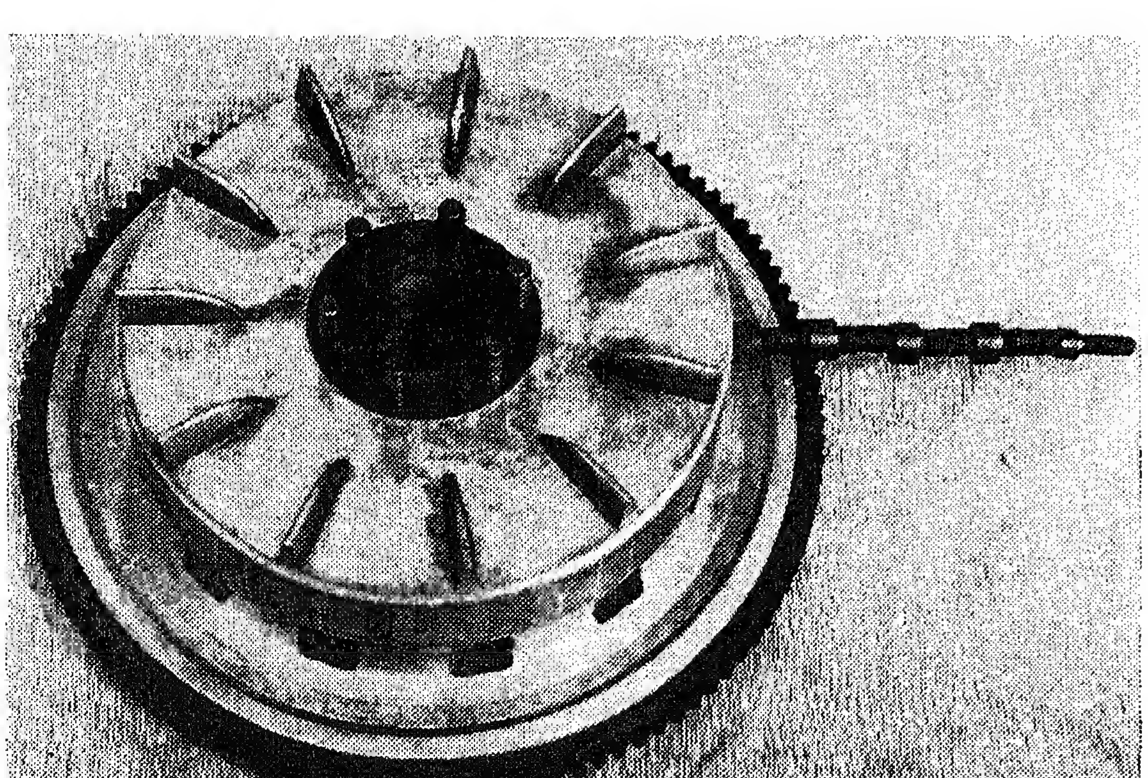


Figure 9. 1.25-kva 6-pole 90-cycle rotor complete with horizontal series of bolts showing the strength of the magnets. This rotor becomes the engine flywheel of the unit shown in Figure 8

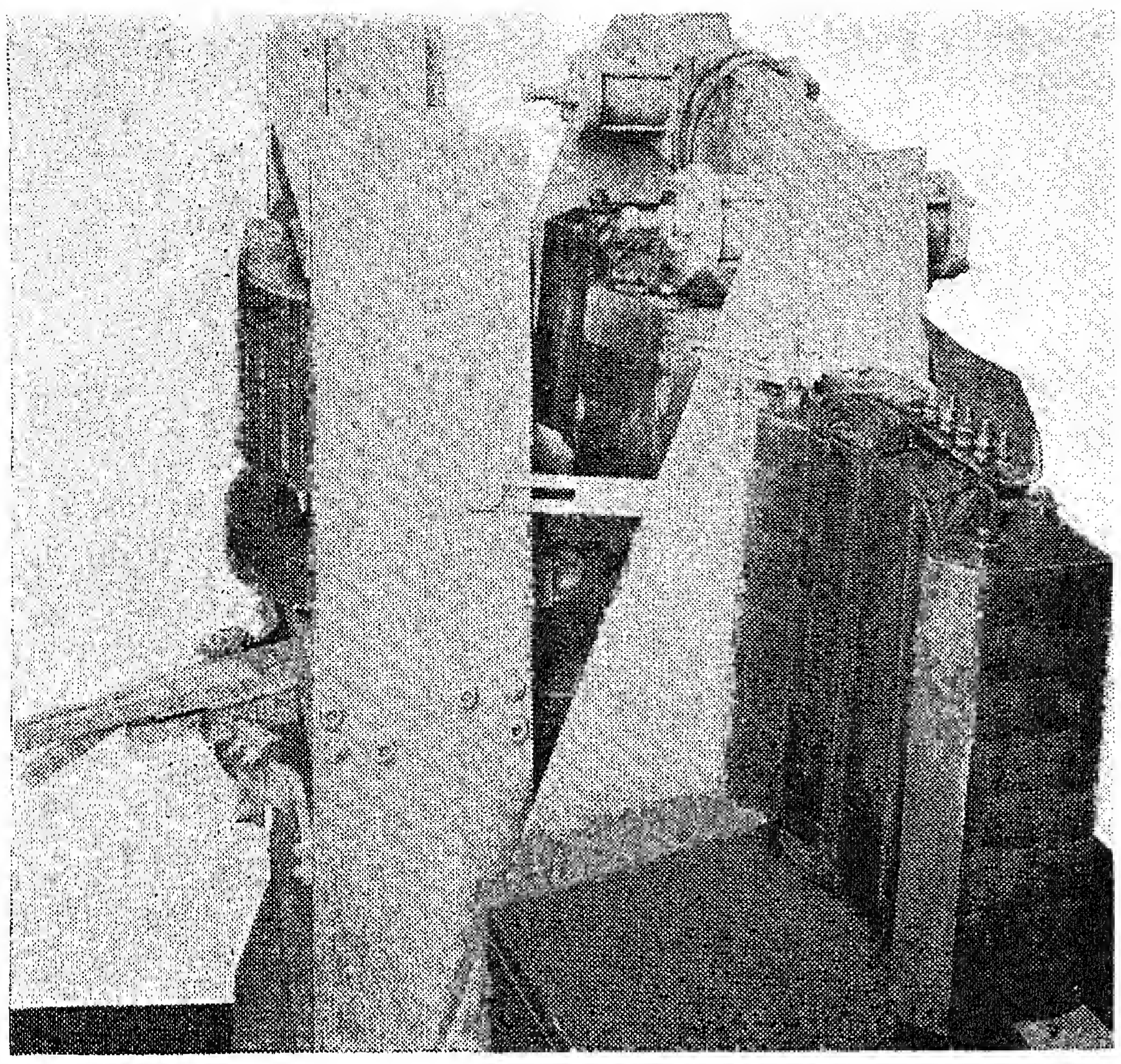


Figure 11. Magnetizer with 20-kva 28-pole 400-cycle rotor installed and 75-kva plate in foreground. The two 4-ton compound hydraulic pushers break the magnetic circuit so that the rotor can be turned easily. A third pusher, not shown, moves the rotor to the vertical position for installation

the capacitor circuit and the phase windings, a bucking or boosting action may be obtained in addition to the usual voltage stability at low power factors.

Improved Voltage Control

The actual voltage control of permanent-magnet generators has been considered impossible except for such complicated methods as mechanically moving the rotor axially in relation to the stator and thus varying the flux interlinkages. However, a simple method of obtaining a small range of effective and accurate control of the generated voltage is by means of a special control winding on the stator.[5] This control winding is simply a uniform series helical winding in the bottom of the slots and over the back of the stator core. No appreciable alternating voltage appears at the terminals of the winding if it is uniform around the entire circumference. Direct current in this winding superposes an undirectional flux on the alternating flux in the stator core. This increases the reluctance of the core and thus lowers the generated voltage as the control current is increased. Tests show this change in voltage to be linear with control winding current. The required direct current may be obtained by rectifying some of the output of the permanent-magnet generator and, if so, the advantage is increased since the control load is added at low loads and removed at high loads, just opposite to a conventional generator which requires maximum excitation for maximum loads. This control may be manual or automatic, but it is limited to approximately a 10-per-cent change in voltage due to the excessive control power required for a greater range. The combination of a control winding for fine adjustment and a compensator for basic voltage stability under a wide variation of load power factor seems at present to offer the best solution to the problem of voltage control. This subject of voltage regulation and control of permanent-magnet generators is a full development project and is barely outlined here.

Applications

A perfect application for small permanent-magnet generators is in rockets and guided missiles where the small size, light weight, high efficiency, positive excitation, and absence of external excitation with moving contacts and associated radio interference are all of vital importance not countered by considerations of cost and difficulty of controlling voltage. Large aircraft applications also will be in this category as soon as better voltage control is perfected.

On a military application of a 15-kva 400-cycle permanent-magnet generator an additional $2^1/_2$-kw 28-volt d-c generator was required with no radio interference. The d-c generator was eliminated by adding a low voltage 3-phase winding to the regular stator winding and rectifying the output under control of a magnetic amplifier.

Another excellent application using a 1-kw 6-pole permanent-magnet generator is the truck refrigeration unit shown in Figures 8 and 9. Here the rotor becomes the engine flywheel and it in turn drives the refrigeration compressor by a semi-flexible coupling. The critical requirements are light weight, short axial length, no brushes, and so forth, which might cause failure, and the ability to generate power at any speed. The generator powers the 3-phase induction motor-driven evaporator blowers in large semitrailers and also supplies single-phase power for charging the starting battery and for all control and for a convenience outlet which may be used for portable lights, electric drills, impact wrenches, and so forth. Advantage is taken of the variable speed range by running the engine (and thus the blowers) continuously but at low speed except when the control thermostat signals for more cooling. Positive excitation is essential since generator failure could result in loss of refrigerated cargo valued at tens of thousands of dollars.

A permanent-magnet generator driven by the static belt is an ideal power source in the high-voltage terminal of a Van de Graaf generator.[6]

Microwave relay stations, especially isolated telegraphic stations, require a maintenance free power unit which will transfer from normal to standby power supply with perfect voltage continuity and negligible change in frequency. Such a power unit is a dual motor-generator set consisting of a permanent-magnet generator normally driven by an induction motor but with a d-c motor on the same shaft for emergency service from battery power. The d-c motor brushes are energized continuously but they are held off the commutator by solenoids to prevent wear. The d-c field is partially excited so that the transfer to d-c power by drop-

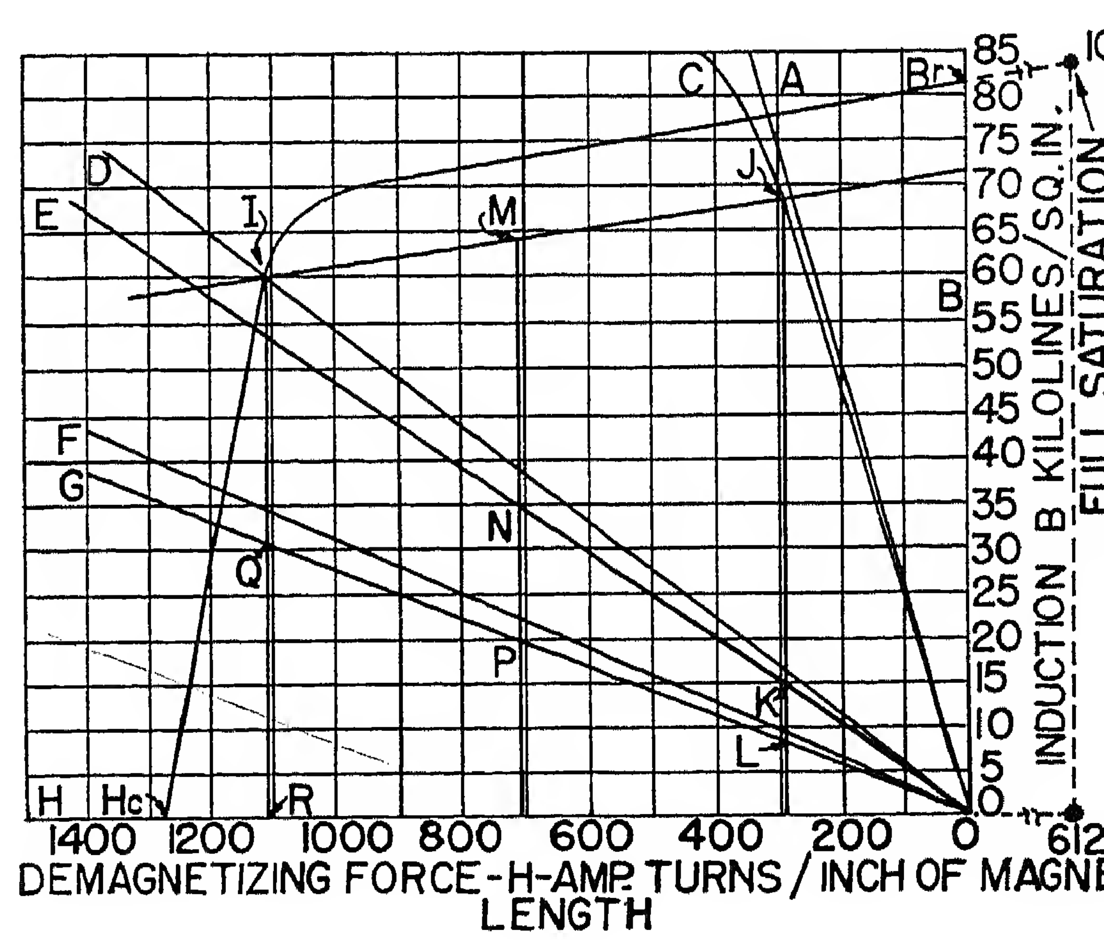


Figure 12. Typical demagnetizing curve for Alnico V with characteristic curves for both air stabilized (D and E) and short-circuit stabilized (F and G) generators

ping the brushes and increasing the field to normal is accomplished with no current surge or speed change. The rotor inertia prevents an objectionable drop in speed for the few cycles required for a change from one a-c source to another or to battery power and this can be reduced still further by the addition of flywheels to the rotor.

Magnetizing

It is possible to magnetize some rotors by applying a suitable voltage to the stator winding with the rotor properly positioned in the stator, but in general it is necessary to use a special magnetizing device with a powerful coil and a heavy steel circuit to obtain full saturation of the magnets. This is especially true of high-leakage rotors where the stator steel is designed to carry only the magnet flux minus the leakage flux and thus cannot handle the much greater magnet magnetizing flux plus the still greater leakage flux. With a large 28-pole rotor, as shown in Figures 1 to 4, it is impossible to saturate completely the entire magnet even with the powerful magnetizer shown in Figures 10 and 11. This machine is used to magnetize all 14- and 28-pole rotors which require more than simple leverage to turn the rotor. The two compound hydraulic pushers each have a 4-ton thrust which proved to be just barely sufficient to release the 75-kva rotor. The magnets are magnetized two poles at a time, one north and one south, separated by a pair of poles, since the poles are too close together to allow space for the steel and winding necessary for simultaneous magnetizing of adjacent poles. Fewer poles, up to at least ten, can be magnetized to full saturation on a single-shot machine.

Stabilizing

The magnets in a generator must be stabilized[7] so that there will be no change in no-load voltage following any normal, or specified abnormal, load condition. A sudden short circuit causes the greatest load demagnetization, hence, it may be used as the stabilizing means, and usually is the means used when the rotor is magnetized in the stator. If the rotor must be magnetized outside the stator, it is necessary to design the pole tips to give the desired leakage flux or to apply a special keeper[8] which will maintain a definite flux as the rotor is moved from the magnetizer into the stator. Such a keeper may be designed to stabilize the magnet properly or it may be oversized and stability obtained later by short-circuiting the windings. The keeper and short-circuiting designs may be used for certain special purpose applications, but for general use the rotors must have complete air stability. Air stability means that the rotor can be removed from the stator without any special keeper device and yet maintain its full stabilized condition and the generator have the same voltage characteristics after reassembly.

The elongated pole tips of the 4-pole rotor shown in Figure 5 act as partial magnetic shunts to increase the leakage as required for best air stabilization. High-frequency low-speed units such as the 28-pole rotor shown in Figure 4 do not require extended pole tips as the leakage with normal pole width may be higher than is desired for best magnet air stabilization.

Qualitative Design Considerations

Figure 12 shows a typical demagnetization curve for Alnico V where *Br* is remanence and *Hc* is the coercive force (the demagnetizing force necessary to reduce the induction to zero).[9] Line *OA* is the air-gap line and indicates the flux in a complete rotor and stator circuit neglecting saturation of the soft iron parts. Line *OC* is the same circuit characteristic with iron saturation included,[10] but the difference is small and may be neglected except for some special designs. The magnet characteristic is not considered so these curves can be calculated from the dimensions of the entire magnetic circuit and the magnetization curves of the steels used in both rotor and stator. Line *OD* is the characteristic of a special high-leakage rotor alone (equivalent to a unit with a stator having unity permeability) and *OE* is the same characteristic except that all flux outside of the air-gap space is neglected (equivalent to a stator having zero permeability). Lines *OF* and *OG* are similar to *OD* and *OE* but are for rotors of few poles and more conventional configuration, which must be stabilized by short circuit to obtain any reasonable output. These are drawn as straight lines since the saturation in the rotor usually can be neglected.

These lines may be plotted on the demagnetization curve as shown, taking into account all leakage flux and the actual length and cross-sectional area of the magnet material. The slopes of lines *OA* and *OC* will change slightly depending upon the type rotor used but only one set is shown for simplicity.

For most efficient use of the magnetic material, it is necessary that the magnet be stabilized near its point of maximum energy product and the point *I* on the curve is taken as just below the peak, see the paper by Fritz Strauss[3] for curves and discussion. If the rotor is stabilized at the point *I*, either in air or by short circuit, the flux thereafter will follow (approximately) the minor loop line back to no-load point *J*, where line *IJ* is drawn through *I* parallel to the tangent to the magnet curve at point *Br*.[7] Application of loads having less demagnetization force than the stabilizing force will retain all action along this minor loop. Ordinate *J* indicates the total magnet no-load flux but only the portion *JK* or *JL* is effective for generation since point *K* or *L* measures the leakage flux which does not enter the stator. At no load, and at high or leading power-factor load with little demagnetization, there may be little difference in generated voltage between air stabilized and short-circuit stabilized machines, but heavy overload at lagging power factor might shift the operation to point *M* on the minor loop and the effective flux would be reduced to *MN* for the air stabilized machine but only to *MP* for one stabilized by short circuit. The correct design of rotors to be short-circuit stabilized is more complicated than for air stabilizing since the slope of the leakage line *OG* must be such that the short-circuit current generated by the effective flux *IQ* is the exact current which will cause demagnetizing to desired point *I*.

No mention is made of subtransient short-circuit peaks with consequent extreme demagnetizing effects since tests show that these generators have comparatively small instantaneous peak values of only about double the steady-state short-circuit current and all designs considered herein have the magnets cast in a solid aluminum alloy body which acts as a short-circuiting turn around each magnet, thus greatly reducing the demagnetizing effect of any assymmetrical current peak. Most designs take further advantage of the aluminum by providing suitable openings in the pole tips and supporting structure so that effective damper bars are cast in place.

The magnetic and electrical design of permanent-magnet generators follows conventional practice except for certain modifications required by the change in source of magnetic energy. These modifications are discussed in detail in Part II by Fritz Strauss.[3]

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No Discussion

Interlaminar Insulation Test for Synchronous Machine Stators

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Synopsis: A method is presented for testing the serviceableness of the interlaminar insulation in the core of a synchronous machine stator. The proposed test requires only the installation of a test winding to magnetize the core. A simplified method is described for the design of the test winding.

A COMPREHENSIVE guide for the maintenance of synchronous machine insulations requires a method for testing the interlaminar insulation. On occasions, operators of synchronous machinery find it desirable to determine if a suspected damaged area of the stator iron is suitable for continued service. Damage to the stator iron may occur in the following ways:

1. Mechanically
   Accidental abrasion in removing or installing the rotor.
   Abrasion during operation by the fracture of an amortisseur bar.
2. Electrically
   Arc burning during a failure of the winding insulation.

A readily applied test to assure the serviceableness of a damaged area will avoid the alternate procedures of replacing or restacking portions of the stator iron.

Test Method

An effective test can be made by installing a magnetizing winding around the stator iron, designed so that the resulting ampere turns will produce a flux in the stator at rated frequency and at approximately operating density. The turns of the test winding should encircle the stator by way of the main bore and outer frame, unless a preferable return route is available closer to the magnetic iron through ventilating space between the stator iron and the outer frame. This winding usually can be designed for a few turns, and can be installed at a location remote from the area suspected as damaged in order to allow free space for an inspector to enter the bore for temperature measurements.

These temperature measurements made during the magnetization of the core will determine the seriousness of the damage. A dangerously damaged area will overheat after a few minutes of magnetization.

Design of Test Winding

The design of the test winding can be based on the turn voltage of the main stator winding by the simple relationship that the volts per turn of the test winding be one-half the volts per turn of the main winding.

This relationship is displayed by Figure 1 wherein (A) shows a simple 2-pole machine having an armature coil with sides directly in the center line of the field poles. The flux produced in operation by the field generates E_t volts per turn in the armature winding. This field flux has two parallel circuits in the stator. Therefore, a magnetizing test winding encircling the stator having $E_t/2$ volts per turn, as in Figure 1(B), will produce an equal number of flux lines in the stator iron.

Data on stator windings supplied to operators of synchronous machinery generally include information by which the test winding can be designed. Use of these data in the following equation will provide the required turn voltage for an effective test winding:

$$\text{Volts per turn} = \frac{E}{2 \times N \times T \times K_p}$$

where

E = rated voltage of armature coil series (for a 3-phase Y-connected machine, E is equal to the terminal voltage divided by $\sqrt{3}$)

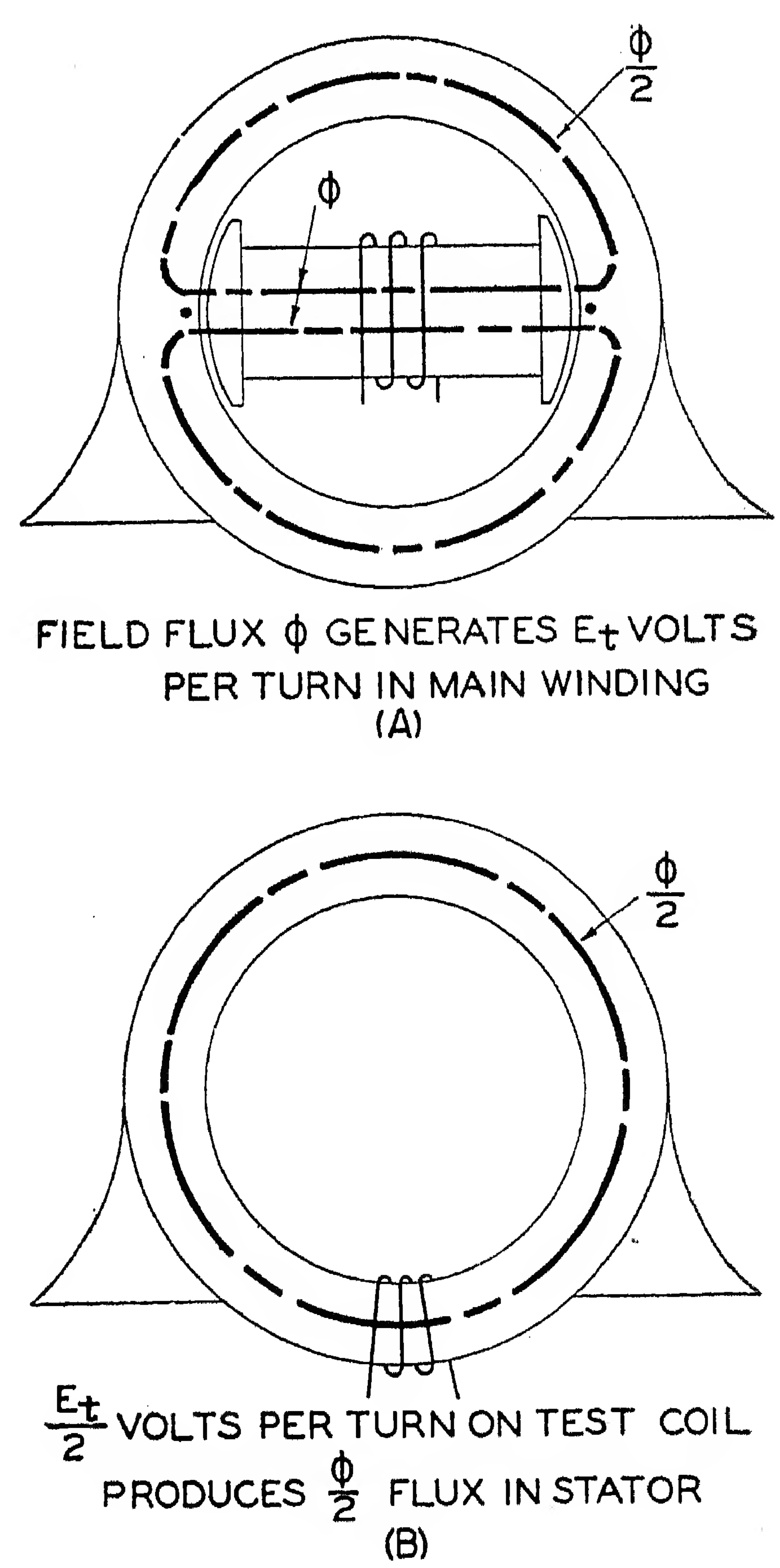

Figure 1. Elementary diagram of core magnetization for synchronous machine

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N = number of coils in series
T = number of turns per coil
K_p = coil pitch factor

The total number of turns of the magnetizing winding is determined by the required turn voltage and the voltage of an available test source.

The ampere requirements for the test winding depends on the magnetic qualities of the stator iron and the mean circumferential length of the stator. Expressing the mean length in terms of the mean diameter, and based on the usual working density of the core iron, ampere requirements can be estimated by dividing 300 ampere turns per foot of mean core diameter by the number of turns required for the test winding. Thereby the required conduction area can be determined for the magnetizing coil.

In some cases it is desirable to install a transformer between an available supply source and the test coil in order to obtain a balance between the required voltage per turn and the magnetizing ampere requirement.

Although the magnetization of the core with this procedure provides an effective test for the integrity of the core insulation, it is recognized that the density resulting may not be equal to that for full-load operation due to such factors as the stator winding distribution and armature load voltage drops. If assurance of the interlaminar insulation is desired at higher densities, the test can be extended to these higher levels by the removal of turns of the test winding. With the same voltage applied, the ratio of the resulting currents will be greater than the inverse squared ratio of the number of turns.

Conclusions

This paper presents a test method readily applied in the field to determine the integrity of the core interlaminar insulation. This test forms a necessary part of a comprehensive program for the maintenance and testing of synchronous machinery insulations.

An effective interlaminar insulation test may be made by encircling the stator with a magnetizing winding whose turn voltage is equal to one-half the turn voltage induced during operation in the stator winding.

The temperature rise of a seriously damaged area will be excessive after a few minutes of core magnetization.

A sample calculation and test results for a modern 75,000-kva turbogenerator is given in the Appendix.

Appendix. Sample Calculations and Test Results for 75,000-Kva Turbogenerator

Rating

75,000 kva, 3,600 rpm, 14,400 volts, Y-connected, three phase.

Winding Data

Two parallel windings, each of eleven 1-turn coils.
Winding pitch = 28/33.
Pitch factor = 0.972.

Calculations for Test Winding

Required volts per turn

$$= \frac{14,400}{2\times\sqrt{3}\times 11\times 1\times 0.972} = 388.$$

A 2,400-volt station service source is available.
Therefore, magnetizing winding should have 2,400/388 = 6 turns.
Mean diameter of stator = 7 feet.
Therefore, probable current = (300×7)/6 = 350 amperes.
Estimated magnetizing kilovolt-amperes = 2.4×350 = 840.
Required conduction area = 250,000 circular mils.

Test Results

With 2,360 volts applied to a 6-turn winding, 335 amperes flowed, resulting in a magnetizing force of 2,000 ampere turns.

Test was extended to higher density by the removal of one turn.

With five turns, 650 amperes flowed, resulting in a magnetizing force of 3,250 ampere turns.

Note: $\frac{650}{335} > \left(\frac{6}{5}\right)^2$

Discussion

L. M. Domeratzky (General Electric Company, Lynn, Mass.): Mr. Tomlinson is to be complimented on presenting information about core testing in such a manner as to be of greatest value to the user of this type of apparatus.

It might be interesting to point out the additional design data usually obtained when such a core test is run in the factory. We instrument the exciting coil with a voltmeter, ammeter, and wattmeter, connected through suitable instrument transformers. Thus loss in the core can be checked and the magnetizing ampere-turns also can be determined. By using an adjustable-tap transformer the flux density can be varied over a considerable range. Flux density in the yoke is determined by looping a single-turn search coil around several packets of laminations and connecting the ends across a low-range voltmeter.

Applying Mr. Tomlinson's procedure to several generators on which core tests have been run, we find that his calculations would have established a flux density in the core about 5 to 10 per cent lower than rated flux density. This is certainly close enough for the purpose intended, namely the detection of bad spots in the core enamel. However, detection of hot spots might be easier at rated flux density or even at higher densities. Therefore, we would suggest that, when suitable facilities are available, a voltage about 10 per cent greater than calculated by Mr. Tomlinson's method be applied. This increased voltage will take care of the leakage flux in the frame, flanges, and binding bands, which we have found to be as great at 8 per cent in some cases. At the increased voltage and flux density, the current requirements also would be greater, in the order of 400 to 500 ampere-turns per foot of mean stator yoke diameter.

H. R. Tomlinson: The author is grateful to Mr. Domeratzky for comparing the results obtained by this test method with a factory procedure where more exact results are obtained with instrumentation. The close correlation between the two is reassuring for the effectiveness of this test method.

The suggestion that the applied voltage be 10 per cent greater than that calculated by the formula is well advised. However, the user need not be too concerned about how close the test flux is to rated density or the exact path of the flux during the test, if the desired end result is only a proof of the integrity of the interlaminar insulation.

The following experience has been obtained by tests based on the formula described in the paper:

1. That core iron visibly damaged was not in serviceable condition without repair.

2. That core iron suspected as damaged was in serviceable condition.

3. That tooth tips without visible damage were not in serviceable condition without repair. These have been found at scattered locations within a stator, even at locations remote from the magnetizing winding.

Hence it may be stated that the proven effectiveness of the described method for testing interlaminar insulation justifies the simplicity of the calculations without other correcting factors.

The user may desire to check a test with instrumentation. This requires only a current reading with a clip-on ammeter and a voltage reading lengthwise across the stator core to check the calculated turn voltage of the test winding.

Relation of Transformer Design to Fire Protection

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Synopsis: This contribution to the discussion of the fire risks of transformers is limited to tests on the materials which enter the apparatus and their probable behavior. Comparisons are made regarding flammability of the solid insulations, as well as the gases evolved from them. These gases are analyzed for chemical composition and volume, and calculations made regarding their probable action in a transformer. The flammability characteristics of the various classes are shown to overlap to a considerable degree, especially for the insulation classes *B* and *H*. This emphasizes the fact that insulation classes are intended to define temperature limitations as regards aging, but do not necessarily classify flammability.

OF THE various types of transformers in use, nothing need be said about oil-filled transformers which constitute the very great majority. It is probably the desire for improvement over their performance in this respect which warrants interest in the behavior of other types.

The first improvement, on which there has now been substantial experience, was the introduction of noninflammable liquid insulations. These have well-demonstrated characteristics of noninflammability and no production of explosive breakdown materials which can contribute to secondary explosions. Extensive internal arcing with much energy, however, can produce disruptive pressures and severe mechanical effects resulting from vaporization. The class-*A* solid materials associated with these liquids do not appear to contribute much to fires because of the inert liquid and gas.

The introduction of open dry-type transformers removed all liquids and reduced the amount of inflammable material in the solid insulations. The amount of inflammable material still necessary, however, led to extensive tests which indicated severe fires could be caused under extreme and rather remote conditions as, for instance, prolonged external short circuits. Caution against the possibility of fire and secondary explosions was voiced. At least one well-authenticated case has illustrated the possibilities inherent in a prolonged external short circuit in a confined space with a resulting secondary explosion.

More recent developments which have advantages in the direction of reduced risk are sealed dry-type transformers using class-*B* or class-*H* insulation with an atmosphere of inert gas. These would seem to make the possibility of fire or explosion exceedingly remote. Nevertheless, it does not appear that the possibility can be dismissed entirely and some examination may be justified.

The behavior of a faulted transformer is to a degree speculative. The location of the fault, energy available, and timing of protective devices all influence the amount of insulation which may be affected. In an external circuit fault at or near the terminals of a transformer it is known that all of the turn insulation, and possibly some adjacent insulation, will be affected to an extent determined by the magnitude and duration of the short-circuit current. In an internal fault probably only a portion of the turn insulation will be involved and the duration probably will be of the order of cycles or seconds. Severe cases of either type are fortunately infrequent, but it is worth while to look at ultimate possibilities.

Methods of Approach

There are in general two methods of approach to this problem. One is to test complete units or at least representative coils under conditions of very heavy overloads or complete failure. Due to cost of samples and time for building and testing, it is practical to test only a few in this manner. Also, the variables tested can be few. Furthermore, while visual and rough over-all effects can be obtained, the tests do not lend themselves to accurate analysis of results.

A second method is to test the components of dry-type transformers. This makes possible many tests, good control of samples and conditions of test, and better analysis of test results. Therefore, this method is selected as the practical approach. Although it does not provide over-all answers and there is uncertainty in extrapolation to full-scale apparatus, we believe that information obtained from such tests may throw light on the problem under consideration.

Materials Tested

Components tested are representative samples of class-*A*, -*B*, and -*B* insulation. These samples have cellulose, asbestos, and glass as base materials and phenolics, polyesters, and silicones as impregnating resins. These are materials that are used currently in dry-type transformers.

The tests conducted are standard American Society for Testing Materials (ASTM) tests plus some special additional tests to demonstrate various characteristics.

Burning Rate Test

A burning rate test is used in accordance with *ASTM Method D-635-41T*.[1] This uses insulation samples 1/2 inch wide and 1/4 inch thick by 6 inches long. Reference marks are made 1 inch and 5 inches from one end. A Bunsen burner flame is placed under one end for 30 seconds and then removed. Elapsed time is recorded for progression of flame between the two marks. If the sample does not burn after the first application, the flame is placed under the sample for 30 seconds the second time. In case the sample does not continue to burn to the 5-inch mark, the sample is reported as "self-extinguishing."

Flammability Test

This is made in accordance with *ASTM Method D-229-49*.[2] Insulation samples 1 inch wide by 1/8 inch thick by 8 inches long are notched in a specified manner 10 notches per inch, and 7 feet of number 30 B & S gauge nichrome wire is wound around the sample in these notches. One hundred ten volts are applied to the ends of the wire. The time interval is recorded

Table I. Flammability

Insulation Class	Seconds to Ignite	Number of Samples that Continued to Burn with Power Off, Per Cent
A	47—118	100
B (3 types)	32—146. Some failed to ignite	56
B (4th type)	Failed to ignite	0
H	Failed to ignite	0

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E. D. TREANOR and L. C. WHITMAN are both with the General Electric Company, Pittsfield, Mass.

Table II. Decomposition Products in Per Cent

	Insulation Class, A	B*	B*	H
Total gas evolved referred to class A	100	77.5	25.0	56
Analysis of gases evolved				
Unsaturated hydrocarbons	7	9.3	0	25.2
Saturated hydrocarbons	0	11.5	40	51.6
Hydrogen	53	3.3	34	18.0
Carbon monoxide	40	15.8	13	5.2
Carbon dioxide	0	60	13	0
Liquids evolved by weight				
Water	5.5	7.9	0	0
Nonvolatile liquid	5.5	5.5	7.7	0

*Two different types.

between the application of the voltage and when the sample bursts into flame.

INFLAMMABILITY OF VAPOR TEST

An insulation sample is placed in a container with a small hole in the cover. The sample is taken of such size that the ratio of its weight to the volume of the container is the same as the ratio of the weight of transformer turn insulation to the normal volume of gas in a typical sealed dry-type transformer (1 pound to 2,400 cubic inches). The container is strongly heated by a large Bunsen burner and a small pilot gas flame held over the hole in the cover. This test is made both with nitrogen and air as the gas to simulate conditions in either sealed or open dry-type units.

DECOMPOSITION PRODUCTS TEST

The insulation sample is placed in a closed system in an atmosphere of nitrogen and the container of the sample is heated to a bright red heat (of the order of 600 to 700 degrees centigrade) until decomposition of the sample takes place. The volume of gas evolved is measured and analyzed as to chemical content. Also, water and other liquids (nonvolatile at 100 degrees centigrade) given off are weighed.

Results of Tests

Burning Rate Test shows all insulation samples, whether class *A*, *B*, or *H*, to be self-extinguishing in terms of the criterion of the test; that is, they failed to burn the required distance. Note that these samples, in order to conform to ASTM Standards, are 1/4 inch thick. It is undoubtedly true that below a certain thickness some of the samples, if not all, will cease to be self-extinguishing.

Flammability (*Ignition Rate*) *Test* shows a marked difference in classes of insulation and considerable variation in representative materials in each class.

In order to show the extreme variation possible in the flammability of class-*B* insulation, the range for three rather common class-*B* insulations are shown grouped together in Table I and a fourth type is shown separately. This fourth type is a special flame-resisting class-*B* insulation not in general use. Results similar to this fourth type also are obtained from a glass mat melamine combination.

Inflammability of Vapors. In these tests, in all but one insulation sample, the gases from class-*A*, -*B*, and -*H* insulations burn continuously. However, a special flame-resisting class-*B* sample (fourth type, Table I) had vapors that burn only initially. After about $2^1/_2$ minutes, vapors evolved from this sample do not burn even with repeated trials of pilot igniting flame. Results similar to this fourth type also are obtained from a glass mat melamine combination.

Decomposition Products Test. According to analysis, gases are evolved from the typical samples tested as shown in Table II.

Some of the extreme variations of decomposition products from class-*B* insulations are illustrated in Table II by listing two different class-*B* materials. Both of these materials are in general use.

In the case of typical sealed dry-type units utilizing materials similar to those tested and assuming complete disintegration of turn insulation due to a fault, we might form some general conclusions.

1. Combustible gases referred to class-*A* sealed unit as 100 per cent and factoring actual quantities of combustible gas might give percentages as shown in the second column of Table III.

In the case of an open dry-type transformer the situation is altered due to the availability of oxygen during decomposition. It is probable that carbon dioxide will be generated instead of carbon monoxide. Also, it is possible that some of the hydrogen will come off as water. The decomposition products will also probably be a function of the temperature that the fault produces as well as the rate at which this decomposition progresses. From the previously enumerated inflammability of vapor tests we do know that practically all the samples tested gave off combustible gases. However, the ratios of combustible gases in open units as affected by insulation class have not been determined.

2. Toxic gases referred to class-*A* sealed unit as 100 per cent and factoring actual quantities of toxic gas might give percentages as shown in the third column of Table III. In an open unit, some or all of the CO probably will be CO_2 which will be nontoxic.

3. Pressures produced by insulation decomposition in sealed units may be divided into two cases. One is that due to a long-duration external fault involving all the turn insulation and lasting at least a matter of minutes. In this assumption it is to be expected that the water evolved does not add to pressure as any such production would have adequate time to be condensed on tank surfaces that would be below 100 degrees centigrade. The second case is that of an internal fault of the order of cycles or seconds duration. Such a fault in general will involve only a portion of the total winding, for example, 10 per cent. Using the same number of pounds of turn insulation for each class and tank volumes that are normally found in current designs, and assuming total decomposition of insulation involved, the last two columns in Table III are arrived at.

Conclusions

1. Insulations of the order of $^1/_4$ inch thick are self-extinguishing irrespective of class after subjection to moderate flames. This may be of some interest in open transformers.

2. Ignition rate of representative insulations shows class *H* much superior with some special class-*B* insulations quite outstanding. Class *B* in general may be better or poorer than class *A*, depending on materials used. This test again is primarily of interest in open air.

3. Most normal samples of insulation, irrespective of class, give off inflammable vapors when heated strongly. A special class-*B* insulation not now in general use shows outstanding noninflammable characteristics.

4. Class-*B* insulation may evolve a greater or a less total volume of gas than class *H*. Also class *B* may have a considerable percentage of noninflammable gas (CO_2) and, as a result, has actually less inflammable material.

5. Class-*B* and -*H* insulations produce only about 10 to 30 per cent as much toxic gas when in a sealed unit as class *A*.

6. Under external fault conditions and assuming a duration sufficient to decompose all turn insulation, it appears that, except for one example tested, any of the classes of insulation tested conceivably may produce pressures that will rupture normal transformer tanks. However, in the case of one class *B*, the tank probably would withstand the pressure produced.

Table III. Combustible and Toxic Gases Referred to Class-A Sealed Unit

Insulation Class	Combustible Gas, Per Cent	Toxic Gas (CO), Per Cent	Pounds Gauge Pressure at 100 Degrees Centigrade: External Fault	Localized Internal Fault
A	100	100	60	12
B*	31	30	47	11
B*	22	8	19	6
H	56	7	35	7

*Two different types.

7. Under localized internal fault conditions involving of the order of 10 per cent of the winding, the transformer tank probably will withstand the pressures that are produced.

8. It would appear that class *B* compares favorably with class *H* as regards flammability and pressures produced in sealed transformers.

References

1. *ASTM Method D-635-41T*, American Society for Testing Materials (Philadelphia, Pa.), 1941, part III, page 447.

2. *ASTM Method D-229-49*, American Society for Testing Materials (Philadelphia, Pa.), 1949, part VI, page 91.

Discussion

M. I. Alimansky (General Electric Company, Pittsfield, Mass.): The application of dry-type transformers in large kilovolt-ampere ratings is growing rapidly. Contributions such as this one to the fundamental knowledge of this line of apparatus are, therefore, important both to users and manufacturers.

This paper deals with fire risks and their differences, depending on the differences in behavior of various insulating materials usually classified as *A*, *B*, or *H*. When one examines the definitions for each of these classes,[1] it is evident that each designer can arrive at his own interpretation. Further, as the authors point out, the flammability characteristics of these classes under fault conditions overlap to a considerable degree.

It is a fact that apparatus designers are employing insulation structures that are mixtures of two or more of these classes. In addition, the rapid stream of new developments in the field of natural and synthetic insulations is providing us with great variety of characteristics which can't always be neatly pigeonholed into a specific class. Then, to top it off, we have the marked improvements resulting from the employment of inert gases, such as nitrogen, as against air.[2,3] How can such a combination of solid and gaseous insulation be classified precisely?

This paper adds further emphasis to the fact that the old classifications of *A*, *B*, and *H* are no longer adequate. A new basis for setting appropriate temperature limits for dry-type apparatus is needed!

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1. General Principles upon which Temperature Limits are Based in the Rating of Electric Machines and Apparatus. *AIEE Standard Number 1*, June 1947.

2. Aging Characteristics of Dry-Type Transformer Insulation at High Temperature, **H. C. Stewart, L. C. Whitman.** *AIEE Transactions*, volume 67, part II, 1948, pages 1600–07.

3. Aging of Class-B Insulating Material in Nitrogen, **H. C. Stewart, L. C. Whitman.** *AIEE Transactions*, volume 70, part I, 1951, pages 436–39.

M. L. Manning (Pennsylvania Transformer Company, Canonsburg, Pa.): This paper presents questionable data for class-*H* insulation compared with class-*B* insulation. The statement in the paper that class-*H* insulation contains 56-per-cent combustible gas compared with 22 per cent and 32 per cent for class *B* (class *A* on a 100-per-cent basis) should not remain unchallenged. Also, a conclusion is drawn that sealed dry-type transformer tanks may rupture under the pressure of generated gas, whether insulation is in the conventional forms of class *A*, *B*, or *H*, if an external fault lasts long enough to decompose all the turn insulation. But, it is stated, in the case of one class-*B* insulating material, the tank probably would withstand the pressure produced.

These statements are contrary to what is known for class-*H* insulation as it is used for sealed-in-nitrogen transformers through 1,500-kva ratings for network, station auxiliary, and substation application.

First of all, the base materials, such as glass fibers or asbestos, must be thoroughly heat cleaned before impregnation with the silicone resins or rubbers. By heat cleaning is meant the removal of binders and lubricants in the material which will decompose at 220 degrees centigrade or higher temperature. The remaining base material is, therefore, 100-per-cent inorganic. The silicone bonding or impregnating resins or elastomers (silicone rubbers) are, therefore, the only remaining constituents to be evaluated. Our class-*H* transformers are given a final 4-hour cure at 250 degrees centigrade (482 degrees Fahrenheit) to remove volatiles in the impregnating varnish.

For truly class-*H* transformers, the turn-to-turn and major insulation is primarily glass fiber impregnated by the silicones. Basically, the barrier construction is silicone-rubber coated glass fiber. Tests on this barrier insulation by application of a 5,000-degree-Fahrenheit oxygen-acetylene torch demonstrate that combustion will not be supported. Silicone rubber becomes beaded silica. The turn-to-turn insulation is predominantly glass fiber bonded by silicone resin. Certainly, no products of decomposition can be considered to be originated from the wire covering.

Silicone resins, cured at 250 degrees centigrade, have been evaluated through 600 degrees centigrade (1,112 degrees Fahrenheit) to determine if volatiles are a problem in nitrogen atmosphere at such high temperature. It was found at 600-degree-centigrade exposure for 2 hours that the noncondensible products were not explosive in nitrogen atmosphere.

The United States Navy[1] has made extensive tests on silicone-insulated electric equipment installed in submarines. It was proved that no health hazard was found through 230-degree-centigrade temperature up to 96-hour operating time submerged. A single motor "burn-out" in a given submarine installation is not believed to increase the total level of carbon monoxide concentration to a dangerous value.

The Naval Medical Research Institute[2] made an evaluation on three electric motors, one class-*B* insulated and two motors insulated with Teflon—a class-*H* fluorocarbon polymer bonded with glass fibers. Tests were made in a closed chamber with a horsepower to volume ratio equal to that found in submarines, without replenishment of air for 96 hours of continuous operation at temperatures up to and including a burn-out above 300 degrees centigrade. The class-*B* insulation consisted of the usual phenolic-resin bonded materials as found in class-*B* transformers. The class-*H* insulation was glass fiber bonded with Teflon resin. The chamber air was analyzed at 24-hour intervals for oxygen, carbon dioxide, and carbon monoxide. For the class *B*, additional analyses were carried out for phenol, formaldehyde, and ammonia. For Teflon, additional measurements were made only for fluoride. At insulation temperatures in excess of 150 degrees centigrade for class *B*, CO concentrations were injurious to health. For Teflon-glass fiber, insulation temperatures above 250 degrees centigrade, CO concentration did not constitute a health hazard. On burn-out tests at temperatures in excess of 300 degrees centigrade, dense smoke appeared at $2^3/_4$-hour duration on the class-*B* motor, and it was enveloped in flames. Samples of the atmosphere at failure of the motor showed 0.36, 0.31, 28.3, and 504 parts per million of phenol, formaldehyde, and carbon monoxide respectively. The insulation was completely burned to a crisp. The Teflon-glass fiber insulation under similar conditions showed less than 0.10 of the carbon monoxide concentration and about 0.2 of 1 per cent fluoride. The motor revealed none of the usual signs of burned-out equipment.

For class-*H* units, transformer tanks properly designed, and with relief devices, should withstand pressures found under extreme conditions of operation. Class-*H* insulated transformers, sealed-in-nitrogen, as described in an AIEE paper[3] and in this discussion, are the answer for elimination of fire and explosion hazards.

The story of class-*H* insulation application to transformers does not end. Plant investment in equipment to make silicones has increased from a fraction of a million dollars in 1944 to twenty-four millions of dollars in 1952, leading to an estimated investment of thirty-four millions by 1954. The most remarkable characteristic of class-*H* insulation is its versatility. It has done work which other previously available insulation could not perform. In its realm lies the future of dry-type transformers.

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3. The Application of Class-H Insulation to Transformers, **Melvin L. Manning.** *AIEE Transactions*, volume 70, part II, 1951, pages 1427–34.

E. D. Treanor and **L. C. Whitman**: Mr. Alimansky has brought out the point that the overlapping of flammability characteristics of the *A*, *B*, and *H* classes of insulations emphasize the need of clarification of insulation classification. New natural and synthetic insulations with a variety of proper operating temperatures, as well as the increasing industrial applications of these materials, make this matter of immediate importance and one that should be actively pursued to a conclusion.

In replying to Mr. Manning, let it be made clear that the authors have presented only factual test data and have made an impartial analysis of these facts to reach the conclusions given. The company the authors represent manufactures transformers and much other apparatus utilizing class-*H* as well as class-*A* and -*B* insulations. In fact, the company is in the business of manufacturing the main constituent of class-*H* insulation, namely, silicone resins. Thus we are of necessity in the position of arriving at the correct answers and the limitations, if any, to these insulations rather than arriving at a particular answer. Thus the percentages of combustible gases evolved at fault temperatures may be surprising to many but are facts which must be faced and dealt with.

In regard to heat cleaning of base materials used, this is, of course, a basic requirement in the case of glass cloth or glass mat in order to get satisfactory impregnation of the resin used. Failure to do this results in a poor insulation not only from the viewpoint of aging and flammability characteristics but also from dielectric qualities. In the case of asbestos, if the type known as quinterra is used, only asbestos fibers are present. If quinargo or some of the other asbestos papers are used, they may contain some cellulose fibers. Materials tested were composed of both insulating materials that we manufactured and which were preheat-treated to remove binders and lubricants, and also some vendor materials of which we have no guarantee that this was done.

The silicone insulations tested were all cured at a temperature in excess of 200 degrees centigrade for at least 12 hours. This equals or exceeds the cure Mr. Manning mentions and removes materials volatile at these temperatures.

The United States Bureau of Mines[1] has published an illuminating report containing data on toxicity and flammability of class-*A* and class-*B* insulations. This report showed wide overlapping of characteristics but in general it was concluded that the volumes of carbon monoxide, cyanides, and ammonia are approximately equal for the materials they tested. Also, "For the same type of resin an inorganic filler will provide a more flame-resistant plastic than an organic filler." A particularly illuminating conclusion made by these investigators is: "It should be noted that an increase in flame resistance will result in a thermal decomposition of the material which will evolve more toxic gases than if the same material had ignited."

The application of an oxygen-acetylene flame to silicone may be taken as a check on the "burning rate test" the authors used. However, in changing the silicone resin to "beaded silica" there is certainly a chemical change with resulting decomposition products which the authors have shown are inflammable when accumulated and which may have some toxic gas components. The toxicity of course will be a function of the parts per million in the surrounding atmosphere and may be dangerous only in confined spaces.

Mr. Manning makes the statement that no products of decomposition can be considered to be originated from wire covering composed of glass fiber bonded with silicone resin. The authors cannot subscribe to this opinion. Under fault conditions the copper windings can be heated sufficiently to disintegrate the silicone resin. Silica is only one of the decomposition products and the other elements present in silicone resin will recombine in various groups, some of which are shown in Table II in the paper. If there is sufficient silicone to provide mechanical bonding and electrical strength in the winding, there will be a sufficient amount to provide these disintegration products.

The United States Navy[2] has shown that for temperatures to 230 degrees centigrade the operation of silicone-insulated apparatus in submarines does not constitute a health hazard. However, at higher temperatures than this, such as would be developed due to external circuit fault or localized internal fault condition in a sealed dry-type transformer, there could be carbon monoxide concentrations which would constitute a hazard to health. It is these extreme conditions and not normal operating conditions that this paper deals with. Knowledge of possibilities under these extreme conditions will allow users to provide safety rules and proper action under such contingencies.

The development of considerable internal pressures such as outlined in this paper should be recognized by the manufacturers so that adequate means of containing these gases without rupture or relieving by auxiliary devices, if demonstrated desirable, can be accomplished.

Silicone insulations definitely have a place in the electrical industry. Their proper application involves not only the use of their desirable high temperature and aging characteristics but the proper balancing of the economics of higher losses due to higher temperatures, and the recognition of the particular operating problems of silicone insulations when subjected to special contingencies.

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2. See reference 1 of M. L. Manning's discussion.

The Re-examination of Temperature Standards for Electrical Insulation

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When insulations were first classified[1,2] as to limiting temperatures, definitions were relatively simple as all of the basic components existed in nature. No synthetic or truly man-made materials were employed at that time. Therefore, the materials fell readily into two classes: organic and inorganic. Temperature limits for the insulation classes were selected at first on the basis of simple tests, but the classification was largely determined by whether organic or inorganic components were used. Subsequently, it was recognized that organic materials fell into two different temperature classes based on whether they were composed entirely of fibrous organic constituents (class *O*) or whether the fibrous components had additional protection from thermal aging by impregnation with varnishes or by immersion in oil. The inorganic components, which were described under class-*B* definition, mica and asbestos, had very obvious uses. Mica was then the only possible inorganic dielectric barrier, whereas asbestos could be used only as a separator on low voltage parts or as a binder and structural member. The question of functional evaluation of insulation did not occur to the engineers who made the original classifications as little or no opportunity for such a choice existed.

With the advent of man-made molecules in numerous synthetic materials suitable for use as electrical insulation, the problem of classifying insulations according to the original definitions has become a very puzzling one. It has become apparent that materials can no longer be defined simply as organic or inorganic even if the intermediate class of semiinorganic is added. It has been found that even truly organic materials as designed and fabricated by the chemists have vastly different levels of thermal endurance, as well as differences in their physical properties both between themselves and from those organic materials occurring in nature. It therefore becomes de-

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sirable to re-examine the definitions of insulation classes in AIEE Standard *Number 1*[1] in order to make a more realistic appraisal of presently available materials and to provide more suitable means for classifying them and the materials as yet to be developed.

Functional Evaluation

It is the author's opinion that insulation temperature limitation should be established with due consideration given the function which the insulation must perform in the apparatus. To accomplish this, it is proposed that the AIEE standards recognize that insulating materials have three distinctly different uses in electric apparatus:

1. To provide dielectric strength by barrier action.
2. To provide physical separation across creepage surfaces or through a porous structure.
3. To provide physical support and surface binding.

It is imperative that the insulation used for the major ground insulation wall on machines provide suitable dielectric barrier action[3] (or that very long creepage surfaces be employed). Turn-to-turn insulations for low voltage may provide only separation for low voltage duty and, therefore, relatively small creepage distances are frequently adequate. However, on the higher voltages, where turn-to-turn stresses are high, a dielectric barrier may be required between turns. Where the insulation's main function is physical support, dielectric barrier action may be of little importance as that is usually provided by other insulation components. The opinion has grown in the electrical engineering profession that an insulation may be entirely suitable for one of these three applications and not for others under similar temperature conditions. When considered from the viewpoint of thermal aging, this leads to the conclusion that materials should be classified thermally according to how well and how long it can perform its intended functions under the conditions of thermal aging encountered on specific applications.

AIEE Standard *Number 1*[1] is subject to many varied interpretations for several classes of insulation. This is particularly true of classes *B* and *H*. As a member of the Working Group revising this standard (for the 1947 issue) the author feels free to criticize this part of the basic insulation concepts and urge further changes to clarify and improve it. One example of the inconsistencies and misinterpretation lies in the use of fiber glass. The definition reads, "Class-*B* insulation consists of mica, asbestos, fiber glass, and similar inorganic materials in built-up form with organic binding substances. A small portion of class-*A* materials may be used for structural purposes only." By one interpretation varnished fiber glass cloth fits this and therefore could be used as major ground insulation. Yet experience had demonstrated that not all varnished fiber glass cloth has suitable thermal endurance to provide dielectric barrier action when operated at the permissible class-*B* temperature. On the other hand, few engineers will question the suitability of fiber glass covered and varnished wire as a satisfactory class-*B* turn or strand insulation where the operating stress is 50 volts or less per turn. In the case of varnished fiber glass cloth, it is important to recognize that it is the insulation level of the varnish as a dielectric barrier after thermal aging which should determine the temperature class of the insulating material or insulating system. In the case of the glass-covered wire, the varnish serves only structural purposes and can be relied on to preserve physical support and spacing long after its varnish film is no longer a dielectric barrier. (In most instances the bond is not intended to contribute to the dielectric strength.)

Current Concepts Affecting Insulation Temperature Limits

Following are several ideas, widely held by insulation engineers, which affect decisions as to changes in temperature limits:

1. Tests on insulation in the "as made" condition are not sufficient to demonstrate its suitability for service under the present temperature class definitions.
2. Suitability for service at specific temperatures should be determined on the basis of the particular requirements depending upon whether the electrical, mechanical, or chemical requirements predominate.
3. Insulating materials may have several different temperature limits depending upon which properties are being utilized in different applications.
4. Accelerated thermal aging tests have become a recognized tool for quick but limited evaluation of thermal endurance. However, accelerated test data should be supplemented with long-time tests at approximately normal temperature extending over an appreciable percentage of the expected or desired minimum life of the insulation system.

Diversified Thermal Aging Tests

Thermal endurance studies by investigators[4–10] have made important contributions to the background of thermal aging of insulation. Information contained in papers by Mathes[11] and Cypher and Harrington[12] present some very interesting and significant data on functional evaluation of complete insulation systems. Such work should be continued and expanded. However, it must be recognized that all of these studies have evaluated specific insulation systems. They do not solve the basic problems of defining and classifying the materials. It would be possible to make broad conclusions from much of the existing test

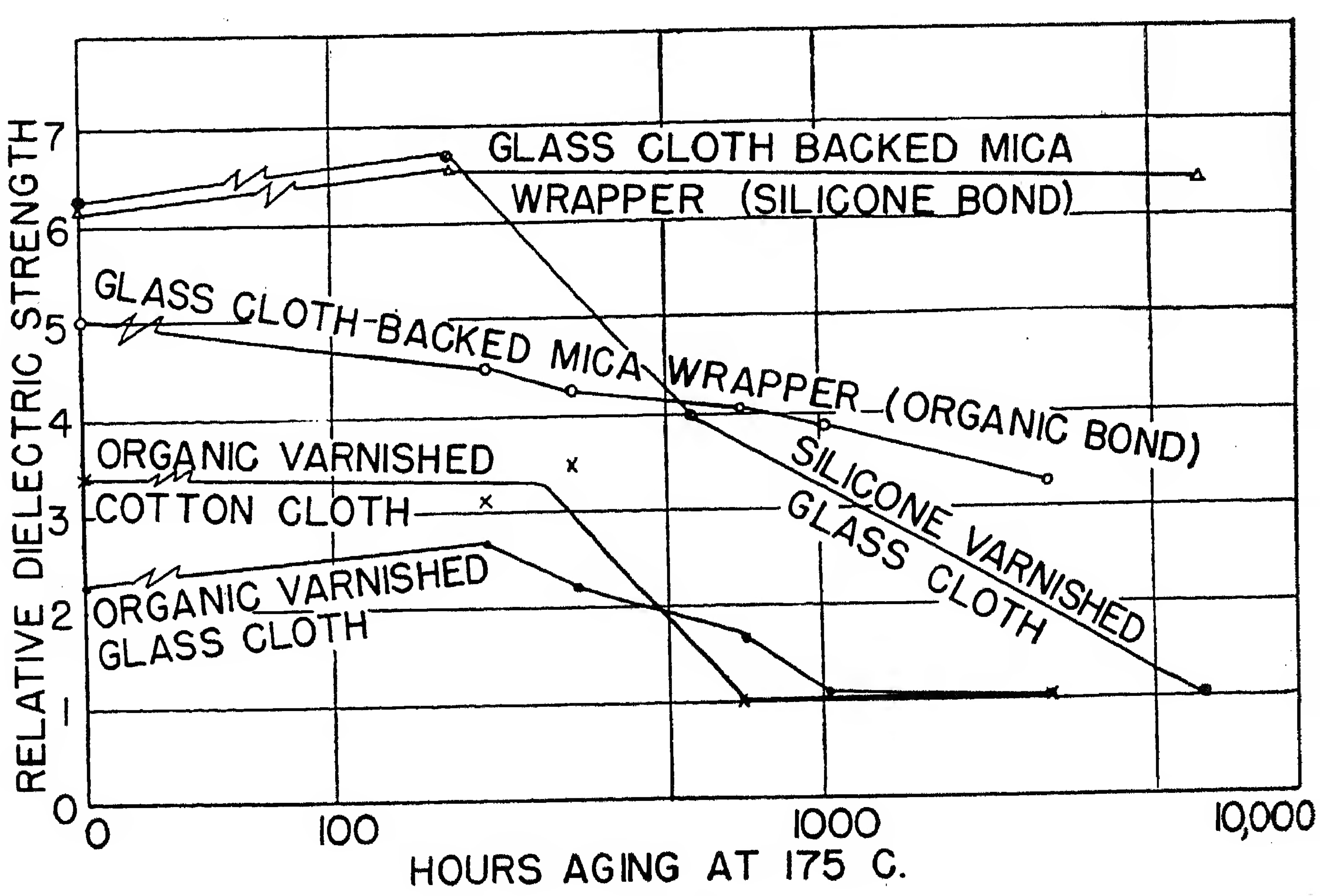

Figure 1. Effect of thermal aging and humidification on relative dielectric strength of insulating wrappers in test bars

data concerning general classes of insulation which may be misleading unless the basic materials are first defined and classified more clearly than the existing Standard *Number 1*.[1] Two points of view are essential to a complete understanding of thermal aging:

1. Individual materials must be classified according to their specific functional evaluation.

2. Insulation systems must be evaluated functionally as a whole for specific duty requirements.

It is important to know the inherent limitations of a material and how thermal aging affects its electrical and physical properties with reference to each of its three possible kinds of usage in insulation systems. When this has been determined, the functional evaluation of an insulation system containing this and other components will take on greater significance. Such a comprehensive program presents many problems and will require the co-ordination of many diverse points of view as well as the accumulation and analysis of a great deal of test data. The first step is to agree on objectives and definitions.

Thermal Aging Data on Dielectric Barriers

As a contribution to this point of view, tests were conducted to study the effect of thermal aging on a series of coil wrappers, such as used for ground insulation on armature coils. These include cotton and glass cloths with organic varnish coatings, glass cloth with silicone varnish and glass cloth-backed mica wrappers with both organic and silicone varnish bonds. These materials were applied to test bars to simulate coil structures. All were evaluated as dielectric barriers using step-by-step 1-minute a-c tests to failure as a function of time at 175 degrees centigrade with humidification after aging. Data are presented in Figure 1 where relative dielectric strength is plotted against time of aging. Unity for relative dielectric strength is the voltage required to breakdown the physical separation where no dielectric barrier action exists.

All varnished fabrics in this series of tests indicated definite signs of thermal aging as measured by loss in dielectric barrier action. The differences between the cloths were only a matter of degree. Referring to the data on organic-varnished cotton and glass cloth, the contribution of the glass cloth is too small to justify differentiating in the standards, yet by some interpretations of AIEE Standard *Number 1* fiber glass cloth with organic varnish coating is considered a class-*B* dielectric barrier.

Both class-*B* and class-*H* mica wrappers exhibit initial values and retention of dielectric barrier action far beyond that obtained from the organic-varnished cloths used in these tests. It is significant that although the initial insulation level is high the silicone-varnished glass cloth (class *H* by definition) lost its dielectric barrier action at a faster rate than the class-*B* mica wrapper.

The information is offered in support of the position that present definitions are inadequate to differentiate clearly temperature classes of insulation and that some revision of AIEE Standard *Number 1* should be undertaken.

Proposed Changes in Insulation Standards

The following suggestions are presented for discussion and development as contributions to broadening and improving insulation standards. It is hoped that they will help in realistic evaluation of current insulating materials and systems. These should be of greater value in fitting newer materials in their proper engineering and economic fields.

1. Recognize the three uses (separation, dielectric barrier, and physical support) of insulating materials in apparatus with respect to evaluation of temperature stability, and differentiate between the requirements for each.

2. Define insulation temperature classes in relation to functional evaluation of materials for specific time-temperature relation to reach certain levels of physical and/or electrical strength.

3. Establish recognized tests for functional evaluation for the thermal endurance of insulation systems for specific service conditions.

4. Require comprehensive testing of new materials and systems under standard test methods before classifying as to thermal endurance rather than reading them into a general class by definition.

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11. Aging of Small Motor Insulation, **K. N. Mathes.** *AIEE Transactions*, volume 71, part III, 1952 (*Paper T2-58*).

12. Functional Evaluation of Motor Insulation Systems, **G. A. Cypher, R. Harrington.** *AIEE Transactions*, volume 71, part III, 1952 (*Paper T2-57*).

Discussion

M. L. Manning (Pennsylvania Transformer Company, Canonsburg, Pa.): This paper presents ideas that should be utilized in determining temperature standards for electrical insulation in transformers, even though Mr. Moses is thinking primarily about motor application.

A transformer coil structure has minor and major insulation. By minor insulation is meant turn-to-turn or component parts not directly connected to ground, and by major insulation is meant high-voltage to low-voltage windings to ground or to low-voltage and ground insulation. The purpose of such insulation is to provide dielectric strength by barrier action, to provide creepage surface, and to provide physical support.

It is evident that insulating materials have to comply with diverse requirements. In addition to the general electrical properties, such as high dielectric strength, low dielectric loss, and high voltage creepage strength, an ideal insulating material has to satisfy heat resistance, good heat conductivity, low water absorption, and good durability, as well as weather resistance. It presents a problem to obtain all of these properties in a single material. Therefore, the plan outlined by Mr. Moses in re-evaluating different kinds of materials according to their thermal stability at operating temperatures and conditions found in service is a step in the proper direction.

The problem resides mainly in class-*B* and class-*H* insulating materials as far as transformers are concerned. For class-*B* transformers, tentative approval is granted for an 80-degree-centigrade average rise by re-

sistance and a permissible 30-degree-centigrade hottest spot rise for a 40-degree-centigrade ambient, making a total hottest spot temperature of 150 degrees centigrade. For class-*H* transformers, of power ratings, substantiating data prove that a 150-degree-centigrade average rise by resistance, plus a 30-degree-centigrade hottest spot rise (about 20 degrees centigrade actually obtained), plus a 40-degree-centigrade ambient, for a total hottest spot temperature of 220 degrees centigrade is permissible.

AIEE Standard Number *1* needs revision for class-*H* insulation. New materials now available, such as silicone rubber or Teflon, were unknown to any extent when these standards were written. The 180-degree-centigrade hottest spot temperature selected is too low. It seems more reasonable to select a hottest spot temperature in the 210- to 220-degree-centigrade range for many class-*H* materials now available.

Aging tests on class-*B* and class-*H* insulations (volts per mil versus days aging at elevated temperatures) support the position that present definitions of materials are inadequate to differentiate temperature classes of insulation. The bonds or impregnants, plus heat cleaning of glass fiber or asbestos, offer the main difference between class-*B* and class-*H* materials.

The design engineer faces a dilemma when he reviews aging curves of class-*B* insulation giving the relationship of voltage strength versus days aging at 150- to 175-degree-centigrade temperatures, even though the base cloth is similar. The varnish impregnants, plus flexing over a 1/8-inch mandrel, make a decided difference. For example, tests on class-*B* 0.012-inch-thick varnish coated glass fiber cloth using 2-inch-diameter electrodes in air at a 150-degree-centigrade temperature show a change from 1,000 volts per mil to 350 volts per mil, no flexing, at the end of 56 days. The same insulation flexed over a 1/8-inch mandrel decreases to 100 volts per mil at the end of 56 days. A class-*H* material, such as silicone rubber coated glass fiber cloth, 0.020-inch thick, aged at 250 degrees centigrade in air and flexed over a 1/8-inch mandrel, maintains a constant 450-volts-per-mil strength for at least 90 days. These examples show strikingly the differences in materials and the importance of thermal stability evaluation at operating temperatures.

The proposed changes in insulation standards outlined in the paper offer a realistic evaluation now necessary for materials.

L. E. Sauer (Westinghouse Electric Corporation, Sharon, Pa.): Mr. Moses has shown the need for re-examination of continuous temperature classification of insulation systems; however, there are several types of apparatus subject to sudden high overloads, which make it imperative that some realistic limits also be established for temperatures under short-circuit conditions. This means that instead of one flat temperature limit calculated with all heat stored in the conductor, there should be numerous temperature limits depending on the type of apparatus and its cooling rate and that the heat storage capacity of associated conductor insulation be considered in calculating the permissible temperature. Also this means there must be a different temperature limit if different starting temperatures exist. These points were recognized in AIEE Standard Number *32*.[1] The curves in Figures 1 and 4 of this discussion will show why consideration should be given to the heat storage capacity of insulation and the rate of cooling in establishing short-circuit temperature limits.

Class-*A* insulation, Figure 1, curve 1: Single 350,000-circular-mil cable stretched out on the floor, heating time 1 minute, thermocouple imbedded so junction is in contact with core strand of cable. Calculated temperature all heat stored in copper 283 degrees centigrade cools to starting temperature in 8 minutes. Curve 3 has the same size cable wound in two discoidal layers per Figure 2; it requires 21 minutes to cool to

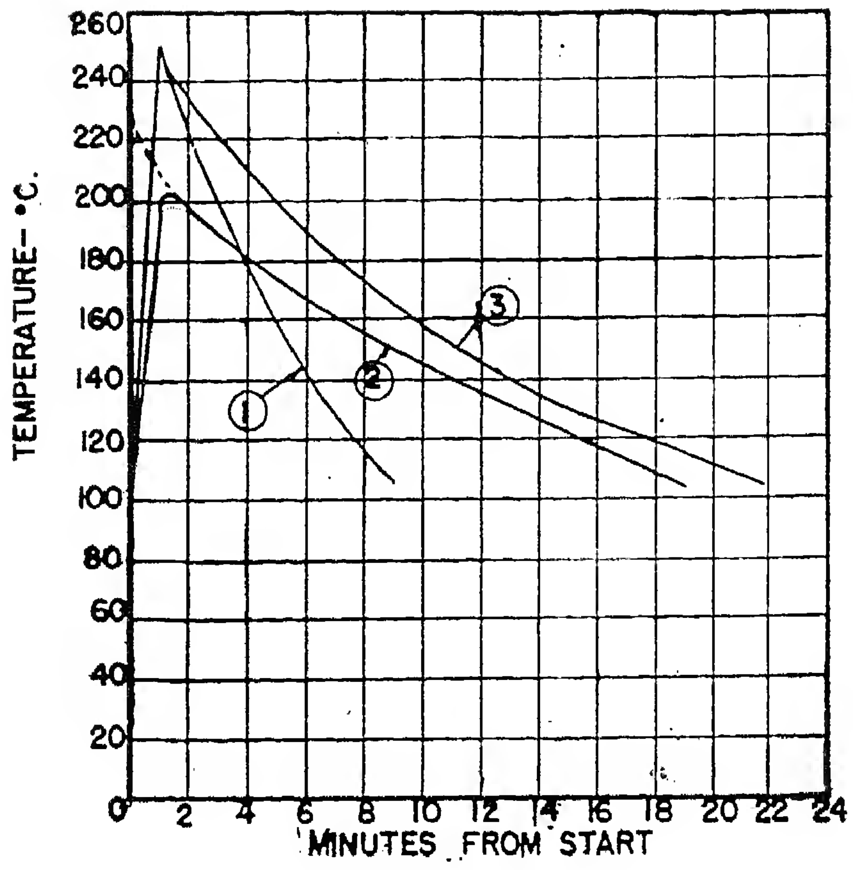

Figure 1

Curve 1: Single 350,000-circular-mil cable stretched out on floor 3,400 amperes 50 times 0.030 Tk. cotton tape, calculated all heat stored in copper 283 degrees centigrade

Curve 2: 350,000-circular-mil cable coil per Figure 3, 3,112 amperes 30 times 0.030 Tk. cotton tape, calculated all heat stored in copper 250 degrees centigrade

Curve 3: 350,000-circular-mil cable coil per Figure 2, 3,400 amperes 30 times 0.030 Tk. cotton tape, calculated all heat stored in copper 283 degrees centigrade

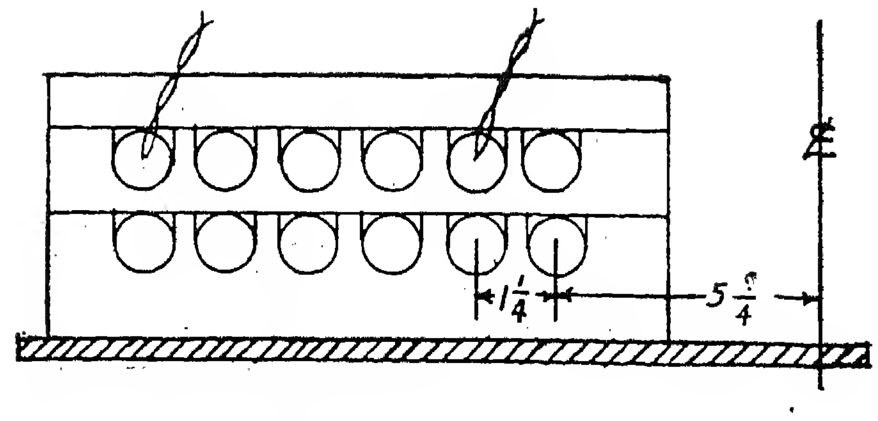

Figure 2. Sample coil with thermocouple buried in contact with core strand of cable

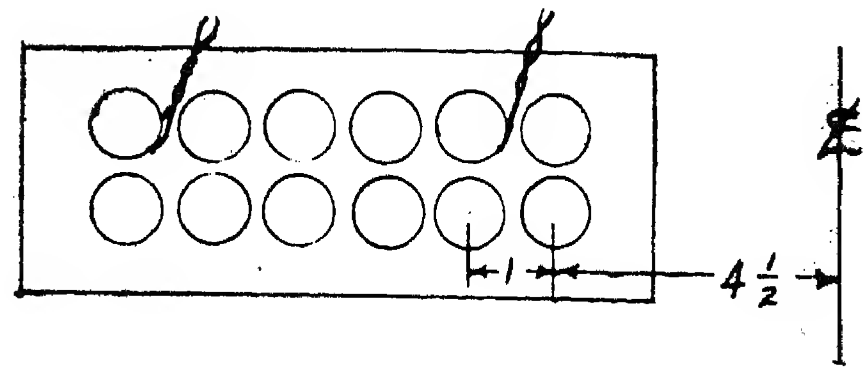

Figure 3. Sample coil with thermocouple between conductor and insulation

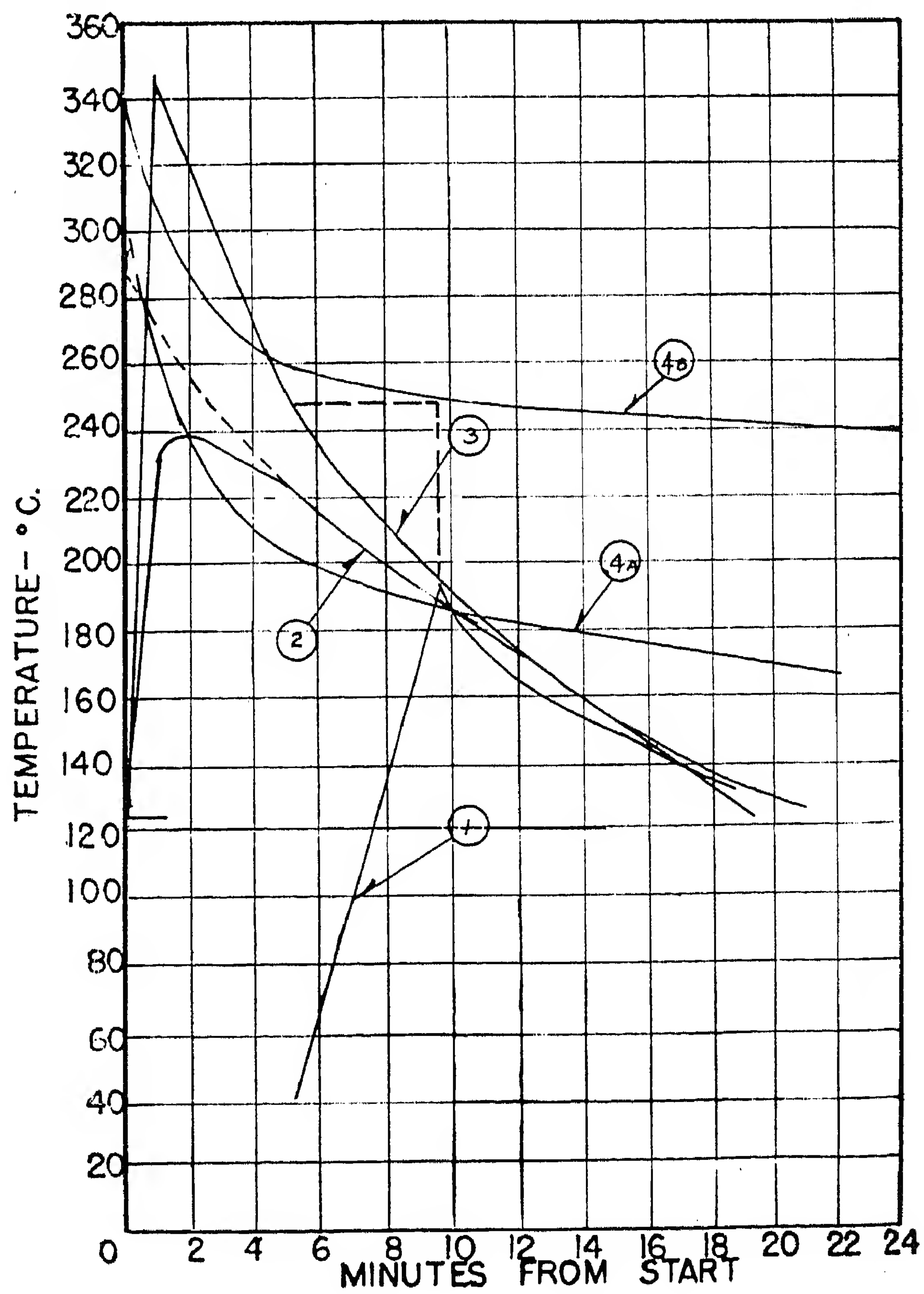

Figure 4

Curve 1: Ten-layer, four turns per layer coil 500,000-circular-mil cable 0.030 Tk. glass tape calculated all heat stored in copper 248 degrees centigrade cooling curve by resistance

Curve 2: 350,000-circular-mil cable coil per Figure 3, 3,682 amperes 30 times 0.060 Tk. asbestos tape calculated all heat stored in copper 350 degrees centigrade

Curve 3: 300,377-circular-mil cable coil per Figure 2, 3,600 amperes 34 times 0.050 Tk. asbestos tape calculated all heat stored in copper 435 degrees centigrade

Curve 4(A): Fourteen-layer, seven turns per layer coil 500,000-circular-mil aluminum cable 4,000 amperes 3.3 seconds 0.030 Tk. glass tape calculated all heat stored in aluminum 358 degrees centigrade

Curve 4(B) starts from end of curve 4(A); 570 amperes applied after 1-minute cooling calculated all heat stored in aluminum 402 degrees centigrade

starting temperature. Curve 3 is for the inner turn thermocouple; the one in the outside turn was 50 degrees lower. The coil for curve 2 was constructed per Figure 3 with thermocouples on the outside of the copper but under the insulation. The camel back of Figure 3 construction is much more pronounced in class-B insulation, curve 2.

Figure 4: Curve 3, Figure 2 construction, shows a calculated temperature rise of 435 degrees for all heat stored in the copper, 400 degrees if the heat storage capacity of the impregnated asbestos tape is included. This still leaves a gap which probably means some heat storage in the supporting columns, radiation, and convection. Curve 1 for 10-layer—four turns per layer neutral grounding coil calculated to reach 248 degrees in 269 seconds at 2,700 amperes. The equivalent normal rating is 30 kva; the cooling curve is by resistance. To make just short-circuit life tests on this small size current limiting reactor would cost $5,000; to duplicate the tests for the four sets of 2-layer coils in Figures 2 and 3 today would cost $2,000. Knowledge of actual cooling from these high temperatures in larger designs is desirable, so in conjunction with design tests to determine the behavior of aluminum cable coils under short-circuit conditions we have obtained cooling curves on a reactor normally rated 180 kva, 14 layers seven turns per layer. Thermocouples were embedded in ten different parts of the coil section so the hottest spot could be determined on regular continuous temperature run.

The 4(A) run of Figure 4 was calculated to reach 358 degrees in 3.3 seconds at 14,000 amperes, all heat stored in the aluminum starting from approximately 132 degrees. Curve 4(B) was calculated to reach 402 degrees for the same heating time with current starting at the end of curve 4(A) cooling, 166 degrees. To check the effect of reclosure on the cooling, 570 amperes was put through the coil after approximately 1 minute of cooling. This makes an appreciable difference in the cooling rate. Experience indicates that the old limits on permissable short-circuit temperatures were probably too low for well-ventilated coils, and just about right for poorly ventilated ones.

I believe AIEE Standard Number *32* was the first step in the right direction of establishing realistic temperature limits, and have hopes that American Standards Association Standard Number *57.16* now under revision will be another progressive step. Certainly the present standards are anything but realistic.

REFERENCES

1. NEUTRAL GROUNDING DEVICES. *AIEE Standard Number 32*, May 1947.

G. L. Moses: It is gratifying to learn of the discussors' general agreement with the proposals contained in this paper. Work has already begun in subcommittees and working groups to incorporate these ideas into the standards. This will not be easy, but should be of value to the industry if it can be accomplished.

Ground Fault Relay Protection of Transmission Lines

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THE majority of transmission-line outages results from faults involving ground with lightning as the principal cause. This fact has long been known and appreciated by system engineers, but it has been substantiated and re-emphasized recently by the comprehensive report[1] of the AIEE-EEI Joint Committee on Line Outages. Consequently adequate ground relay protection is important and necessary to maintain the high degree of service as required today.

While there are numerous papers in the AIEE literature on specific ground relay schemes and problems, there does not seem to be available a general review of the principles and practices on this important subject. This paper will attempt to present such a review with the hope that it will be of value to system and protection engineers.

Most transmission systems in this country are grounded, usually solidly, although some are grounded through resistance, reactance, or impedance. They generally have a multiplicity of grounding points. Consequently, except in a few cases, ample ground or residual currents ($3I_0$) and voltages ($3E_0$) are available and used for operating the protective relays. I_0 and E_0 are the zero sequence quantities. Negative sequence current (I_2) and voltage (E_2) are also present on ground faults and can be used in combinations with the zero sequence quantities to detect and isolate their faults. The term "ground faults" refers to faults between one or two lines or phases, and ground (or any point connected to the ground wires or earth). Three line-to-ground faults will not operate ground relays unless there is sufficient unbalance to produce measurable zero sequence. Thus this fault generally is considered as a phase fault.

None of these discriminating quantities for ground faults are present to any appreciable degree during normal balanced 3-phase operation. This important and fundamental principle makes it possible to apply separate relays or elements for ground protection which are set very sensitively and below 3-phase load. Thus, separate ground relays are applied almost universally if the system is grounded.

It should be recognized that the phase relays, which must be set so as not to operate on maximum load, also may operate on some of the ground faults. However, this generally is not considered in the application of ground relays. Whenever the phase relays do operate, they provide additional backup protection.

Here it becomes evident that the method of symmetrical components is an invaluable and necessary tool to the relay engineer. Dr. Fortescue's powerful invention has made possible much of the advancement attributed to the relay art, and in particular to ground relaying. Constant reference will be made to positive, negative, and zero sequence quantities as they exist on line-to-ground and 2-line-to-ground faults.

General Types of Ground Relays

Any transmission line in a grounded system can be protected by a combination of one or more of the following types of relays:

1. Time overcurrent using
   a. inverse time relays
   b. constant or fixed time relays
2. Time directional overcurrent using same relays as in item 1
3. Instantaneous overcurrent
4. Instantaneous directional overcurrent
5. Directional reactance
6. Pilot differential using
   a. pilot-wire relays
   b. directional-comparison (current or voltage) carrier relays
   c. phase-comparison carrier relays
   d. transfer trip relays

The order of these is roughly that of in-

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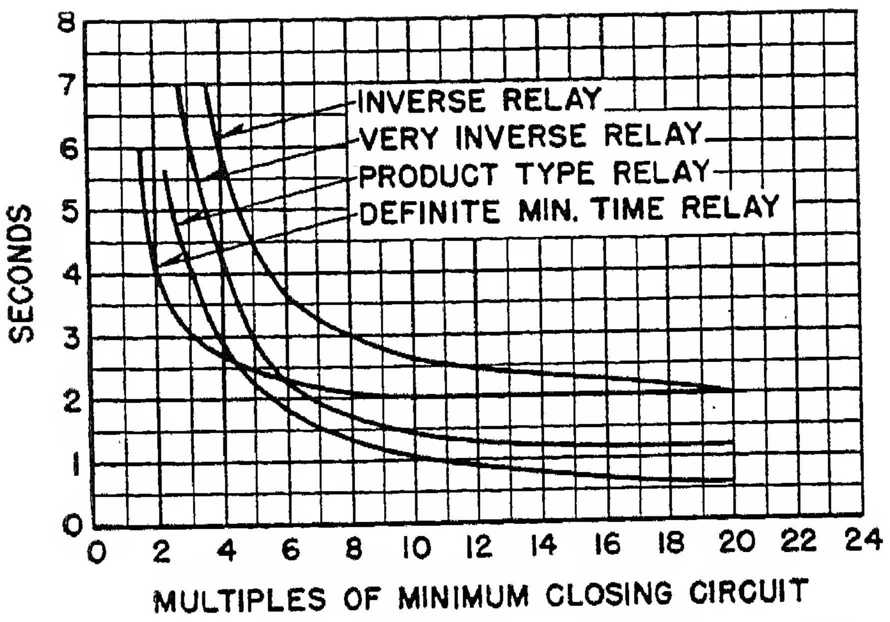

Figure 1. Typical time-current characteristics of the various inverse relays

creased speed of operation and increased cost. The problem of application is to determine the type that will give adequate protection for the degree of service required of the circuit. A direct answer to this for any particular system is not possible as there are too many variables such as the characteristics of the system, the operating practices, and economics, as well as the requirements of the protective system.

Polarizing Methods for Directional Ground Relays

Where a directional characteristic is required, two schemes are used. One is to employ a separate element to indicate the direction of fault power. This element controls the overcurrent element by various means which in turn provides the time current characteristics shown in the inverse, very inverse, and definite minimum time curves of Figure 1. The directional element time, being fast in relation to the overcurrent time, usually is neglected in the operating time of the relay. The second scheme is to obtain directional characteristics from the overcurrent element itself by bringing out both sets of operating coils. Thus the operating characteristic is a function of the product of two currents or one current and one voltage. This gives the product-type relay curve of Figure 1.

For ground directional indication, a reference is necessary to determine whether the line current is into the line or into the bus. The zero sequence voltage ($3E_0$) can be used since it is always in the same direction regardless of the location of the fault except for special cases of induction. This is known as potential polarization. The polarizing voltage is measured across the open corner of a star grounded-broken delta potential transformer. Either the main or auxiliary set of transformers can be connected in this manner. The latter connections are illustrated in Figure 2.

Another method of obtaining a directional reference is to use the current in the neutral of a grounded star-delta power transformer bank. The connections for this method also are shown in Figure 2. This is current polarization, and potential transformers are not required. Where there are several banks at a station the current transformers in all the neutrals should be paralleled to provide polarization as long as at least one bank is in service.

Three-winding transformer banks can be used for current polarization as long as there is a delta winding or tertiary and one star-grounded winding. If there are two grounded-star windings and delta tertiary, then both neutrals must have current transformers paralleled with their ratios inversely proportional to the bank voltages.

The grounded neutral of an autotransformer with delta tertiary may or may not provide a correct polarizing source as the current in the neutral can be either up or down or zero for faults on the high-voltage side. It is always up the neutral as in two winding transformers for all low side faults. A current down the neutral and out into the line for high side faults appears the same to the directional element as current up the neutral and in to the high side of the bank for low side faults. This reference or neutral reversal is a function of autotransformer impedances and ratio and of the interconnected low side zero sequence system. The determination of this is covered in the text books on symmetrical components and is outlined in reference 2.

In most cases the direction of the zero sequence current in the 3-winding or autotransformer delta tertiary is always in the same direction and can be used for polarizing. In this case a single current transformer is connected inside the delta to measure $3I_0$ circulating there, if there are no external connections to the delta. Otherwise, with external circuits from the delta, a current transformer is required in each delta winding. The three are connected in parallel and are necessary since unbalanced load or fault current can give incorrect polarizing current.

Within the last few years several cases of current reversal in the delta have been reported so this method must be checked before it can be used reliably. The reversal occurs because one branch of the equivalent transformer circuit is negative and is larger in magnitude than the total interconnected system. Practically this means that a small capacity transformer bank (high reactance on a common base) is connected to a very large (low reactance) solidly grounded system. Where this delta reversal occurs, other means for polarizing must be found.

At stations where there are no grounded power transformers, potential polarization is, of course, the only method applicable. Where a grounded neutral is available,

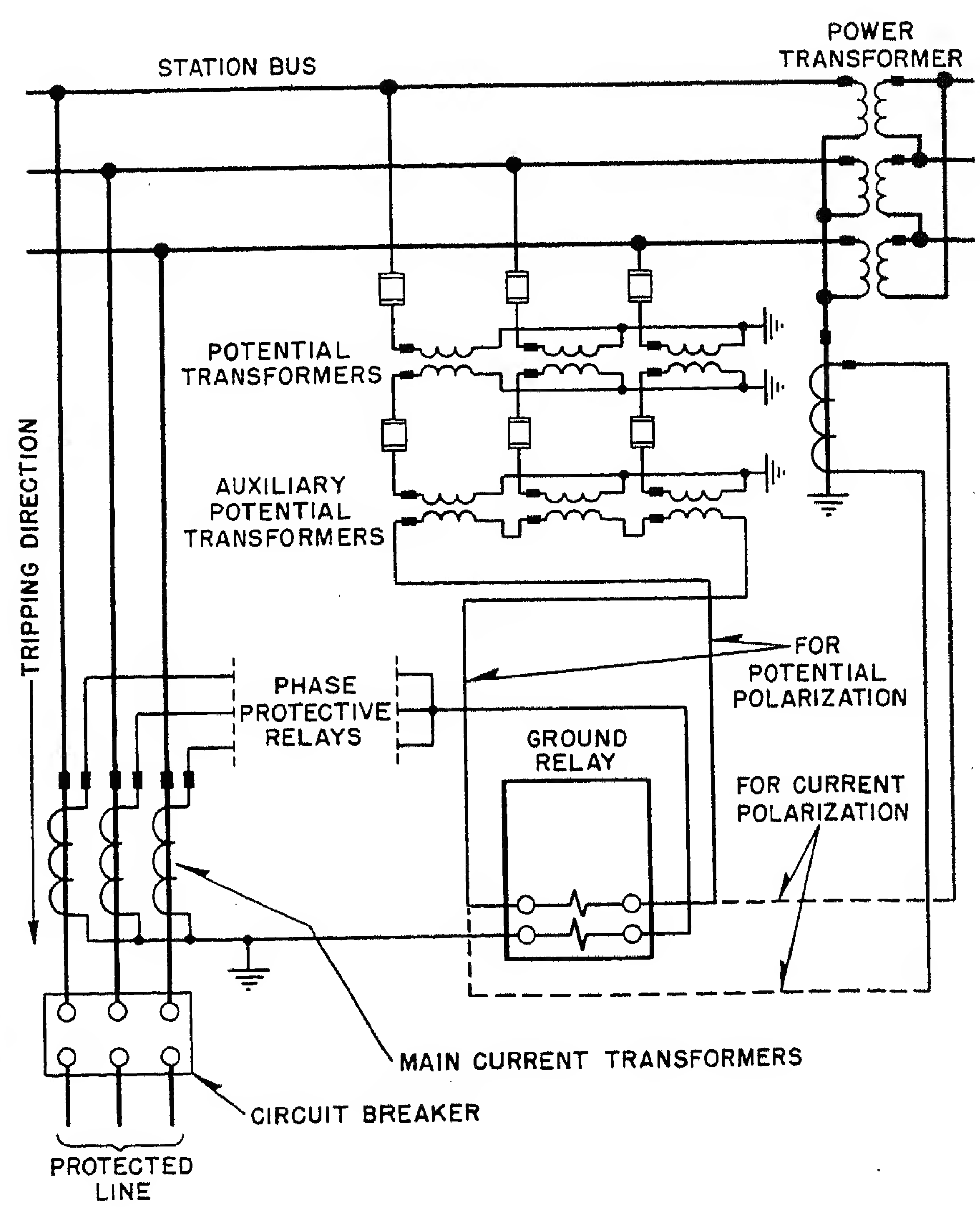

Figure 2. Typical connections for a directional ground relay using either potential or current polarization

either current or potential polarization can be used. However, current polarization generally is recommended at these stations as the residual voltage may be quite small particularly where there are large banks and a large residual current available. The zero sequence voltage is maximum at the fault so that minimum polarizing voltage will occur for the remote fault where most of the zero sequence voltage is consumed as IZ drop in the line.

Ground relays having means for simultaneous current and potential polarization are being considered and may become standard in the future. Such arrangement automatically is more complex and hardly seems justified for many applications. It is not practical in the product-type relay where the time of operation is a function of the polarizing quantities.

Directional ground relays utilizing negative sequence current and voltage are used frequently. The overcurrent element operates on zero sequence from the main or line current transformer in the usual manner, but the directional element is energized by negative sequence through a current and voltage filter. This relay is generally applicable, particularly in a solid grounded system. However, it seldom is used except where zero sequence quantities are not available for polarizing. This is a matter of economics as the additional filter unit is required for negative sequence. Typical applications are at ungrounded stations where only open delta potential transformers exist or where no potential transformers are available except on the opposite side of a star-delta power transformer bank. The negative sequence pickup of this element is quite low, but for correct application some knowledge of these quantities should be available.

The exception to voltage and current polarization is on parallel systems where the zero sequence networks are isolated. Then a ground fault on one system can induce current into the other along the parallel and cause the current and voltage to reverse in the ground sources at one end. In these cases negative sequence directional relays are used since the amount of negative sequence induced even with untransposed lines is very small and negligible.

Inverse Time Relaying

The large majority of ground relay systems use combinations of the first two types with inverse time relays. These may provide the primary protection for the lines or be used as a secondary system for backup protection. The relays all include an induction disc element operated by current, the product of two currents, or the product of a current and a voltage, and are available with numerous time-current characteristics. Some typical curves are shown in Figure 1. For high-current close-in faults the relays should operate fast. However, for lower current faults near or at the remote end of the line, they must operate with time delay if they are to co-ordinate with bus differential relays or other line relays radiating from the remote bus. In this system time delay is the only means the relay has of determining the location of the remote fault. The relay current for all practical purposes is the same whether the remote fault is on the line itself, on the bus, or on other lines out of the bus. In the first case, the relay should operate to clear the fault, but for the other two cases it should not operate until after the other protective relays on the bus or lines have had an opportunity to clear the fault. In this manner each relay selects or co-ordinates with all other relays which may operate on any given fault. Such selective settings and operation are necessary in order to isolate the trouble area with a minimum number of circuit-breaker operations.

Most transmission systems, particularly in the higher voltage classes, are grounded at more than one point. In these multiple-grounded systems directional-overcurrent ground relays rather than straight overcurrent ground relays are applied. This considerably reduces the complex selectivity problem. Consider the typical system of Figure 3. Starting at station *A* and using directional-type relays which will operate only when fault power flows into the line section relay 1 must select or co-ordinate with relay 3, relay 3 with relays 5 and 6, 6 with 8, 8 with 10 and 1, 5 with 11, and 11 with 9 and 1. Around the loop in the counter clockwise direction, relay 2 must co-ordinate with relay 10 and 9, 9 with 7, 7 with 5 and 4, 4 with 2, 10 with 12, and finally 12 with 4 and 6. In a loop system every relay must select with at least one other relay. Since the time setting of each relay depends upon the time setting of other relays, a definite final setting for any relay can not be determined except by trial and error. This is further complicated by changing system conditions which will give varying operating quantities to the relays. Usually this can be resolved into two limits designated as maximum and minimum operating conditions. These provide two sets of fault data for which the relay must be set to co-ordinate. In cases where more operating conditions with various lines in or out of service must be considered, the relay operation must be reviewed for each such case.

The product-type relay can give incorrect sequential operation on parallel lines with mutual under certain conditions. Consider two lines on the same tower connecting bus *A* to bus *B*. Both busses connect to grounded systems. A ground fault on line 1 near bus *A* would be cleared fast by the bus *A* line 1 relay and circuit breaker. After this circuit breaker opens the effect of the mutual reactance in combination with the rest of the zero sequence system can cause a larger product to exist for bus *A* line 2 relay than for bus *B* line 1 relay. This is because more of the fault current is supplied by the remote or bus *A* ground source than the near or bus *B* source. The result is that the bus *A* line 2 relay on the unfaulted line operates incorrectly since the fault should be cleared by the bus *B* line 1 relay. This does not always occur and it depends on the system zero sequence constants together with the circuits used for polarizing. A fault study with ground faults on the line side of an open circuit breaker permits checking for this condition. The other directional overcurrent or non-product-type relays are not affected in this manner because the current through the faulted line is always equal to or greater than the current through any other line.

To make selective ground relay settings on any system, the following information is required:

1. A ground fault system study under the maximum and minimum operating conditions (and others if necessary). This should give $3I_0$, $3E_0$, I_2, and E_2 fault values and their distribution over the system for a line-to-ground fault at each station bus. Total fault currents are of little use, as the division of these values in the protected and adjacent lines and in the neutrals of the grounded power transformer are required to determine the current available for operating the relays. The voltage and negative sequence values are not always required, but they are recorded easily during a study on an a-c or d-c calculating board. If they are omitted and required later during the relay study, they are often very difficult to obtain. Frequently the knowledge of the redistribution of the fault values for a fault on the line side of an open circuit breaker is invaluable in applying relays where the protection with all circuit breakers closed is difficult, or to determine if product-type relays can be applied to parallel lines with mutual.

2. The line and neutral current transformer ratios which are to be used to energize the relays at each location. Where potential is used, the potential transformer ratio is required.

3. The circuit-breaker operating time. This information is required to provide the proper interval between the overlapping relays. This is called the co-ordinating time interval and it includes the circuit-breaker operating time, plus a margin of safety factor for any difference in current transformer performances, relay characteristics, fault values, relay coasting time, and so forth. For 8-cycle circuit breakers the value of 0.4 second is commonly used, while for the older 30-cycle circuit breakers, 0.75 second is used. Lower values can be used, but with discretion.

Two general methods of setting the relays are in use. One employs a chart for tabulating the system and relay data together with the setting and time of operation for various faults. This method is described in detail in reference 2. Another uses co-ordinate paper on which the relay time curves are plotted for several locations under various system conditions. A graphical picture of the relay co-ordination thus is obtained.

It should be apparent that considerable time and study are required to set inverse time relays on a large system. After the relays are set, it is always possible that some unexpected system condition will upset the co-ordination. These are some of the reasons for the trend toward directional reactance or pilot differential relaying. While the first cost of these relays is higher, the saving in man-hours in making or rechecking the settings soon can offset the difference.

Comparison of Time Curves

With various inverse time characteristics, such as shown in Figure 1, the question arises as to which to use. Unfortunately no definite answer is possible without a detailed study of the particular system and its requirements. It is the general practice to use the same curve characteristic throughout the system to minimize the possibility of incorrect co-ordination under some extreme conditions. The process of making selective relay settings amounts to stacking the time-curve characteristics one over the other. If various time curves were used from station to station there is always the danger of them crossing or coming too close for proper co-ordination at some point unless this is checked carefully at all points. Of course this danger exists when using the same time curve but to a lesser extent. On the other hand, with the same time characteristic the fastest protection cannot be realized for all lines and conditions.

In general terms, the more inverse-type time curves tend to provide improved or faster relay times for ground protection. As a rough rule, on loop systems, it is necessary to have approximately the same interval as the co-ordinating time interval between the relay operating time for faults at the near and far end of the line being protected. Hence, with 0.4-second interval between relays, each relay itself must operate approximately 0.3 to 0.5 second faster for the near fault than for the far fault. The definite minimum time characteristic of Figure 1 is relatively flat, becoming more inverse at the higher lever settings. Thus, to obtain co-ordination around the loop, high time lever settings would be necessary with the resulting unsatisfactory relay times of 1 to 2 seconds or more. This illustrates why definite minimum time relays seldom are used for ground relaying.

Induction-type relays with inverse, very inverse, and product-type time curves, as shown in Figure 1, are in common use as ground relays. In many multiple-grounded systems the very inverse and product-type curves which are very similar provide the fastest relay times with the product type generally proving more satisfactory. This relay was designed specifically for ground relaying and is enjoying wider use as time goes on.

A time characteristic that is quite inverse can be used because the variation of the ground fault current for maximum and minimum system conditions usually is less than the corresponding variation for phase faults. In addition, this variation is less for the remote fault than the near fault unless the lines are very short. These result because the zero sequence impedance does not vary with changes in generation (unless transformers or lines are switched) and is two to six times the positive sequence impedance for the transmission-line section. Thus for the close-in fault where the current is large and more variable with generation changes, the relay is operating on the flatter part of the inverse curve. On the other hand, for the remote faults the current is smaller and less variable because of the line impedance. For these faults the relay is working on the inverse part of its characteristic where variation is more critical. The net result is that the severe close-in faults can be cleared rapidly and with less time difference under all operating conditions. More time variation is permissible for the less severe remote faults.

Backup Protection

The previous discussion has been based largely on providing minimum operating times for ground faults in the immediate line section. This is first line or primary protection unless pilot or different-type line relays are used. Back-up or secondary protection for the next adjacent section is desirable. In many cases this may be quite difficult without slowing up or compromising the primary protection. It is then necessary to evaluate the backup against the loss in primary protection for the continuity of service required.

Adjacent line backup protection is particularly difficult on multiple-grounded loop systems. The problem can be illustrated by reference to Figure 3. Relay 2 at station *B* has as its primary protective zone the line from station *B* to station *A*. As backup it should cover the lines to stations *D* and *E*. For faults on these lines, the ground source at station *A* (and in some cases the other lines from *A* radiating to other ground sources) raise the voltage at this bus so that less of the ground current flows to the fault through the relay at *B*. With very little current at 2 for faults near circuit breakers 8 and 11, the relay at 2 must be set sensitively if it is to operate. Then for faults on the line near circuit breaker 1, the relay will be operating on the flatter part of the in-

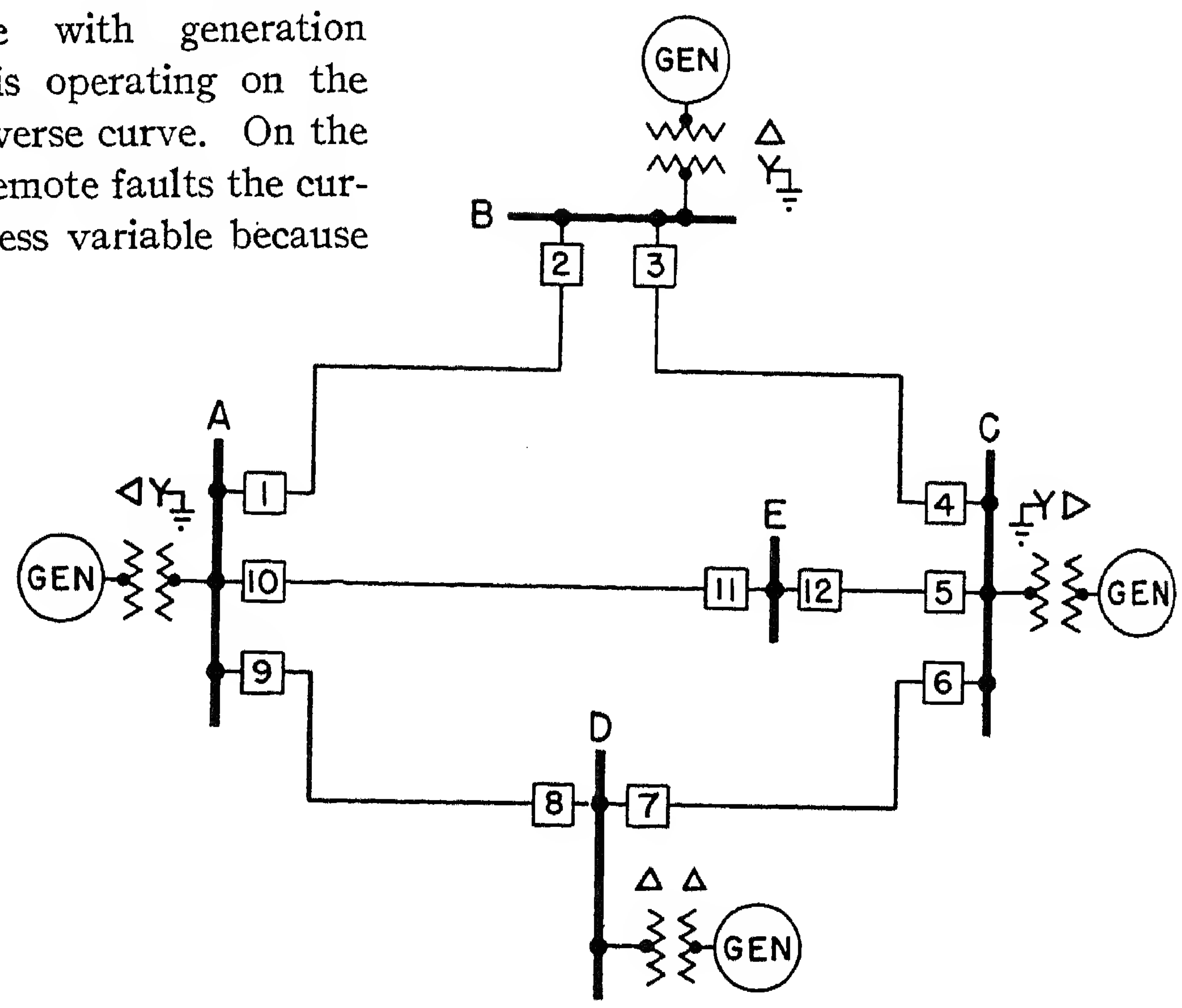

Figure 3. A typical multiple-grounded transmission system

verse curve and the time must be longer in order to co-ordinate with relays 9 and 10. As a result advantage of the inverse curve is lost which would give fast relay time for faults near 2.

On systems of this type none of the inverse time relays will provide the fastest possible protection over both the immediate and the adjacent sections. Generally the very inverse characteristic of Figure 1 will give the fastest operating time over the immediate or primary line with slower times over adjacent lines. On the other hand the inverse characteristic will provide improved backup time but slower times over the primary line.

Sometimes, opening the remote circuit breaker on the adjacent line (such as 8 or 11) will change the distribution of fault current. If the relay current increases, the backup coverage may be improved. However, the current can decrease and make backup protection even more difficult. In doubtful cases this effect should be investigated.

It should be recognized that many of the general statements here represent trends and experiences on specific systems. There are always cases to be found where they may not completely apply.

Constant or fixed time relays are used primarily for "last resort" backup relays in two general ways. One is to connect the relay in the neutral of the grounded transformer bank. If the ground current persists beyond the time allowed for the other relays to clear the fault, then the neutral relay operates to remove the transformer, or bus, from the rest of the system. An inverse time relay with a long setting also could be used in this manner.

The other method operates a time relay whenever any of the trip circuits are energized. If the fault is not cleared in a definite time, all of the circuit breakers in the associated area are tripped. This protects only against a "stuck" circuit breaker.

Instantaneous Relaying

Instantaneous-type relays unless controlled by carrier, pilot wire, and so forth, can be used only to supplement inverse time relays. One exception to this is where the remote terminal of the line terminates in a star-delta transformer bank. An instantaneous ground relay can be set very sensitively and will operate for all ground faults on the line and into the bank. Ground faults on the far side of the bank cannot cause operation since zero sequence does not pass through the transformer bank. In other words, this circuit inherently provides selectivity so that co-ordination with the remote circuit breaker by time is not necessary.

Many transmission lines of moderate or long length provide inherent directional overcurrent discrimination as the result of the variation in fault current magnitude for the near and remote faults. Where this occurs, the addition of instantaneous overcurrent elements to the inverse time relays will give relay operating times in the order of 1 cycle for a good portion of the line with a minimum of additional cost.

The nondirectional type of instantaneous relay is incorporated most often in the same case with the inverse relay. These are known as instantaneous trip attachments. Where a directional characteristic is required, two separate elements are used, one for sensitive directional indication and the other to operate at high speed when the current exceeds a set value.

The principles of applying and setting supplementary instantaneous relays are illustrated in Figure 4 for circuit breaker number 1 at station A. The relay should operate for the close-in fault 1 and for faults as far as possible toward bus B but not operate on fault 2 or for fault 3. Thus the magnitude of these currents flowing through the relay as shown in the figure must be known. Fault values 2 and 3 should be the maximum obtainable under any operating condition while the value for 1 can be either maximum or minimum. Both values for fault 1 show the approximate coverage or the relay under the two operating conditions.

To use instantaneous relays for circuit breaker 1, the magnitudes for fault 1 must be appreciably larger than for either fault 2 or 3. As a general rule, the relay pickup is set about 25 per cent above the maximum current for fault 2 or 3. Where the relay current for fault 3 is in the order of, or less than, that for fault 2, a nondirectional relay can be used. However, if the relay current for fault 3 is large, a directional instantaneous relay is required and is set only considering faults 1 and 2. Of course, if the relay current for fault 2 is large compared to those for 1 then instantaneous relays are not applicable or the setting is so restricted as to be of little value.

The application of supplementary instantaneous relays also may permit resetting the inverse time relays so that the backup coverage is extended. This is done by setting them more sensitively so that the close-in faults covered by the instantaneous relay are beyond the recommended operating of the inverse relay. This procedure should be used with caution lest the removal of the instantaneous relay for maintenance or test find the inverse relay unable to adequately protect the system as required and without damage.

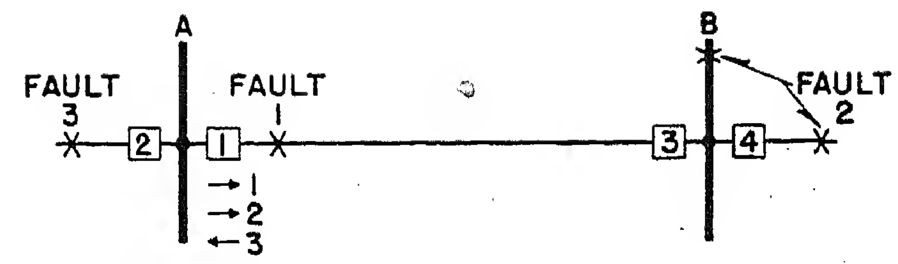

Figure 4. Fault values required for applying instantaneous ground relays on a transmission line

Directional Reactance Relaying

Relays responsive to the reactance or distance from the relay location to the fault provide instantaneous protection for the majority of the transmission line relatively independent of the variation in generating capacity or to line switching. Unfortunately there are inherent difficulties in the measurement of impedance or reactance during ground faults. As a result distance ground relaying is not in wide use. Recent advancements in the art have provided a ground reactance relay more universally applicable and many of these are being installed throughout the country. By the nature of the problem, they are more complex than the widely used phase distance relays. However, it seems quite probable that the trend in the future will be to use more distance measuring relays for ground protection, perhaps with inverse time, if not distance-type relays for phase protection on lines that do not justify pilot-type protection.

For ground faults, it would seem logical to compare the line-to-ground voltage to the line current to indicate the distance to the fault. However, this ratio does not provide a constant proportional to the transmission-line impedance (or reactance) from the relay to the fault because the zero sequence impedance of the line does not equal the positive or negative sequence impedance. As a result the ratio of the line to ground voltage to line current varies considerably when system changes external to the line section in question cause a variation in the proportions of positive, negative, and zero sequence current flowing through the relay. In other words, any change in the composition of the relay operating current causes an error in the distance measurement since the sequence component composition of the line voltage drop does not vary in the same manner.

Another difficulty arises from the necessity of using reactance for ground

fault measurement. Reactance is required because of values of arc resistance and ground contact resistance can be very high for ground faults. Actual resistance components do not affect a reactance element, but the current fed to the fault from the remote terminal will produce an apparent reactance component when it is out-of-phase with the relay current. This either adds or subtracts to the reactance from the relay to the fault and causes the reactance element to either underreach or overreach. With an instantaneous reactance element set to protect a line section, it should not be permitted to overreach the remote bus and trip for faults on the bus or lines from the bus. Thus instantaneous reactance elements are generally set for only 65 to 75 per cent of the line section compared to 85 to 90 per cent for impedance phase relays. Another factor in the shorter setting is the unknown accuracy and variations in the zero sequence reactance which includes the reactance of the earth return.

There are two methods of eliminating the errors in ground distance measurement caused by the zero sequence impedance not being equal to the positive sequence impedance. These were outlined in detail by Lewis and Tippett[3] in 1931. One is to add residual current to the line current on the operating side of the element to balance out the excess voltage drop caused by the zero sequence current. This is known as current compensation. The other method subtracts the positive and negative sequence voltage drops from the line-to-neutral voltage, leaving only the zero sequence voltage drop to be balanced against the zero sequence current from the relay to the fault. This is voltage compensation.

A ground reactance relay using the first or current compensation method has been available for around 20 years, but never used extensively except in a few systems.[4] Three ground relays, each receiving one of the phase currents, are necessary. Because load current flows through the relay it is necessary to supervise the reactance relay with an impedance-type element whose setting in turn depends on maximum load carried over the line. As a result the application of current compensated ground relays is limited usually to low ground contact resistance and low earth reactance lines, such as are available on a steel tower line with solidly bonded hardware, and with one or more ground wires.

The development of sequence filters and their use in selecting the faulted phase has made practical the use of voltage compensation ground relays.[5] For line-to-ground faults the faulted phase is selected and its line-to-neutral voltage minus the line positive and negative sequence voltage drops is applied to reactance measurement elements. Inasmuch as the operating or reactance elements receive only zero sequence quantities, this relay is independent of load current as a ground relay should be, and is de-energized except during ground faults. This important feature makes it possible to design the relay to operate very sensitively and on a low amount of energy, hence its more universal application to transmission lines.

None of these relays operate on two line-to-ground faults. These are left to the phase relays which have no difficulty as the fault is partially a phase fault. With high-speed ground relaying for most of the line section, it is probable that the number of two line-to-ground faults will be less. This will be true if most faults start as one line to ground and spread to two or more phases if not cleared rapidly.

All of the ground reactance relays provide three zones of protection as has been common in the phase distance relays. The first zone is set within the line section and operates instantaneously. The other two zones are set into the adjacent line sections for protecting the remaining part of the line not covered by the first zone and for backup coverage of the adjacent lines.

Pilot Differential Relaying

Pilot differential relaying refers to the various systems where the conditions at one terminal are compared with similar conditions at the other terminals to determine whether the fault is within the transmission-line section or external. The most important advantage is that all line terminals can be relayed simultaneously and instantaneously for all faults, both phase and particularly ground. This isolates the fault quickly and before it can spread appreciably. Also it permits immediate restoration of the line by automatic reclosing on the basis that most line faults are flashovers that can be cleared by de-energizing the line for a few cycles. As a result this type of relaying is becoming the standard method of protection of important transmission lines.

For the comparison, some form of communicating channel between the remotely separated line terminals is necessary. This can be pilot wires (usually two or one pair), carrier frequency superimposed on the transmission line, or microwave beamed between the terminals. Audio tones modulated on carrier and audio or high-frequency tones on microwave also have been used for relaying.

There are two different relay schemes in common use. One is the power-directional comparison scheme generally used with power-line carrier although it has been used over pilot wires, audio tones on carrier, and microwave. The other is the phase-comparison scheme, originally developed for pilot wires, and later modified for use over carrier or microwave channels.

Directional instantaneous relays are used for ground protection in the directional-comparison scheme except where the line or tap terminates in a delta winding of a power transformer. Then instantaneous voltage relays are applied. The instantaneous elements are set sensitively so that they operate for faults beyond the remote bus. For these external faults a blocking signal from the remote terminal prevents tripping. For internal faults the directional element at all terminals closes and tripping takes place through these contacts and the sensitive instantaneous contacts in the absence of a blocking signal. This scheme is applicable to any number of terminals.

The phase-comparison scheme compares the phase position of the currents in the remote terminals. This is accomplished by using a single-phase output voltage proportional to the three line and neutral currents. Such a voltage is obtained from a composite sequence filter or mixing transformers. It is saturated to limit its magnitude variation for large changes in the fault currents.

In the pilot-wire systems, the voltages at the terminals are compared differentially over two low-voltage pilot wires, such as a telephone pair. This provides protection for all internal faults with the order of 1-cycle relay operating time. Generally pilot-wire relaying is applied to protect short lines and cables where the average length is 3 to 5 miles, although pilot wires from 15 to 30 miles are in service. Where one terminal is ungrounded, the pilot-wire scheme can trip this end from the current fed to the fault through the grounded terminal or terminals. This does not require potential transformers and is of considerable advantage in special cases. The 2-wire phase-comparison pilot-wire schemes are applied only on two and three terminal lines.

For carrier or microwave, similarly derived voltages are used to transmit alternate half-cycle blocks of carrier from each terminal. The phase position of these blocks are compared every other half-cycle with the phase position of the local signal to determine whether the fault is in-

ternal or external. This system is limited usually to two terminal lines although it can be used for three terminals.

The application and limitations of the two carrier-type relaying systems have been outlined in detail in reference 7. Many of the limitations apply particularly to phase protection rather than ground so that the system used will be determined largely by the phase relay requirements.

With microwave channels being used for relaying, a new field of applications for various combinations of directional instantaneous overcurrent or reactance relays is opened. These are known as transfer trip systems. They differ from carrier in that a tripping signal rather then a blocking signal is transmitted over the communicating channel. Such systems have not been applicable with power-line carrier because an internal fault might short-circuit the signal at a time when it is required for tripping a remote circuit breaker. Several possible schemes are already in use.[8]

Where the majority of faults on a transmission line are line to ground, opening only the faulted phase or phases would clear the fault and leave the other phases to carry synchronizing power. Thus the stability limit is increased and the shock on the system reduced. These advantages lead to the development of single-pole or selective-pole carrier relaying systems using either directional- or phase-comparison type relays. The single-pole scheme operates to open and reclose only the faulted phase on a line-to-ground fault. On all other faults all three phases are opened. With selective-pole relaying, one phase is opened and reclosed on line-to-ground and line-to-line faults, two phases on two line-to-ground faults, and all three phases for 3-phase faults. A number of installations of these types have been in successful operation for more than 10 years.

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Discussion

M. A. Bostwick (Portland General Electric Company, Portland, Oreg.): Mr. Blackburn's review of ground fault protection is of considerable value as it directs attention to most of the problems that are encountered when selecting ground relays. While most of these considerations have been discussed previously, it is of value to have this complete review in one publication.

He also points out one consideration that had not been previously brought to our attention, namely, the fact that the delta winding of an autotransformer is not an infallible source of polarizing current.

It occurs to me that the value of high-speed relay protection, with relatively sensitive ground relays, can be emphasized further by consideration of the cost of repairing lines after burn-downs. While the cost of service and inconvenience are probably major considerations, it is also of interest to note that the minimum cost for repairs will be in the order of $250. The probable cost of a burn-down is many times this amount. Consequently, the installation of ground relays will provide a most economical and desirable addition to the line protection.

R. W. Dearing (The Detroit Edison Company, Detroit, Mich.): Mr. Blackburn is to be congratulated for his comprehensive treatment of this subject and it is felt that his paper will be a useful guide and reference to the practicing relay application engineer.

It is realized that Mr. Blackburn was precluded from being very specific in many points of his discussion, due to time and space limitations, but it seems appropriate to point out that there are a few points of sufficient general interest to warrant further clarification or amplification.

One of these points appears in the first section of the paper, with respect to the co-ordination of phase and ground relays. There is an implication here, which we are sure that the author did not intend, that there is little need for co-ordination between phase relays and ground relays. In practice however, this is not invariably the case. There are numerous examples of short-circuit locations on transmission systems where the relative multiples of pickup inherent in the operating quantities furnished to phase overcurrent relays at one location and ground relays at another location must be observed carefully and evaluated in determining the proper relay settings to maintain adequate selectivity. This is especially true, of course, during the development stage of a ground relaying system on a transmission network where some of the relaying terminals have not yet been equipped with ground relays. Moreover, even on a "completed" system, the need for careful co-ordination may continue to exist where some lines may be protected with relatively sensitive directional overcurrent phase relays.

In the discussion of polarizing methods for directional ground relays, the author calls attention to the fact that at certain relaying terminals the residual voltages available for directional element polarization may be quite low for remote terminal ground faults. Frequently, in fact, this residual polarizing voltage may be too low to assure positive closing of the relay directional element at a value of fault current corresponding to the overcurrent element pickup. Several instances of this nature have existed on the system with which the writer is associated, where conversion to current polarization is not economically or structurally desirable. This difficulty has been surmounted successfully by changing the mechanical spring bias on the ground relay directional element so that the directional contact is normally closed, and adjusting the relay to open its directional contacts with a voltage applied which is equal to, or preferably less than, the minimum residual voltage obtained at the relaying point for such ground fault locations in the reverse direction as will draw pickup current through the tripping element of the relay.

On the same system, there are instances of twin circuit line pairs which inductively couple branches of isolated zero sequence networks. Fortunately, it has been possible up to the present time to continue with the application of zero sequence directional relays without recourse to the more complicated negative sequence elements and filters by setting the tripping element pickups well over the computed values of induced circulating residual currents so the expected reversal in indication of directional elements cannot cause false tripping. This has been done admittedly at the cost of some reduction in sensitivity to ground faults but still with what is considered an adequate margin of safety in system protection.

Eric T. B. Gross (Illinois Institute of Technology, Chicago, Ill.): This valuable paper is essentially a review of ground relaying in systems which are so grounded that ground faults are short circuits. The reader who looks for up-to-date information on sensitive ground relaying will find it in a recent AIEE Committee Report.[1]

As pointed out by the author, distance relaying of ground faults has not yet found wide application in this country. The reason for this fact may be the need for two sets of relays, one for interphase faults and the other for ground faults which scheme necessarily involves high investment costs. It is interesting to note that simple and relatively cheap equipment using a minimum of three distance relays for both interphase and ground faults has been in use in Europe for nearly 30 years. The main improvement in distance ground relaying "current compensation" is due to Jean Fallou;[2] it was first used in 1929 in an early installation in France.

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J. L. Blackburn: The discussors' comments are appreciated and are valuable additions to the paper. Mr. Dearing has clarified an important point regarding co-ordination between phase and ground relays.

Mr. Dearing's special adjustment of the directional element is interesting, and it is regretted that more details were not included. Apparently, he is able to do this because of particular system conditions which provide a smaller residual voltage for faults in this trip direction than for faults in the nontrip direction at the overcurrent element pickup current value. This may not always be the case, hence this arrangement must be used with caution.

Effect of a Modern Amplidyne Voltage Regulator on Underexcited Operation of Large Turbine Generators

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IT HAS long been known[1–3] that the use of a continuously acting regulator will permit stable operation of a synchronous generator in regions where such operation is not possible with either manual control or regulators with dead band. (This is often called operating in the dynamic region.) It has long been recognized that, due to saturation effects, operation at lagging power factors is not a problem as far as steady-state stability is concerned. Because of the growing necessity for, and desirability of, operation at leading power factors, a need has developed for a quantitative evaluation of the improvement to be expected from a modern continuously acting regulator. This paper proposes to show the steady-state limits of steam turbine generators which may be achieved using a standard, commercially available, continuously acting regulating system (amplidyne buck-boost type), and compares these limits with those yielded by regulators having dead band (such as the indirect acting rheostatic type) to show the degree of improvement.

As an adjunct to this problem, the use of a very low short-circuit-ratio generator has been considered as another means of evaluating the effect of the regulator. This comparison is significant because at present the size of high-speed turbine generators is increasing rapidly and the use of lower short-circuit ratio results in more kilovolt-ampere capacity from the same frame size with corresponding reduction in first cost.[1,4]

Conclusions

The following conclusions have been drawn:

1. Use of a modern, continuously acting, buck-boost amplidyne regulator greatly increases the steady-state stability limit of turbine generators in the underexcited region.

2. The present trend towards lowering the short-circuit ratio of large turbine generators is entirely sound from a steady-state stability standpoint, providing a modern, continuously acting regulator is used.

As a rule of thumb, use of such a regulator with a generator having one-half the usual short-circuit ratio will give a stability limit comparable to that realized by present-day turbine generators using regulators having dead band.

Discussion of Results

The results of this study are shown in Figures 1 through 4. Figures 1 and 2 apply to a large turbine generator of present-day design. Figures 3 and 4 apply to a machine with one-half the short-circuit ratio. For such a machine x_d and x_q would be doubled while the open-circuit field time constant, inertia, and damping would remain about the same. The new value of x_d' would be between one and two times that for the normal short-circuit-ratio machine. Results are shown for both values of x_d'. The kilovolt-ampere base was selected as equal to the maximum guaranteed turbine rating.

Figures 1 and 3 are a direct comparison of the effect of short-circuit ratio using an external or system reactance of 0.4 per unit which is higher than normally encountered. Figure 3 shows that there would be no steady-state stability margin at this value of x_e when operating at rated power, unity power factor, and low short-circuit ratio unless a modern amplidyne voltage regulator were used. An amplidyne regulator, however, gives a good stability margin over the operating range of the generator. Comparison with Figure 1 shows that, over the operating range, the steady-state stability limit with an amplidyne regulator is the same as the limit using regulators having dead band on turbines of normal short-circuit ratio.

Figure 2 shows that for machines of normal short-circuit ratio use of a modern amplidyne regulator allows operation over the operating range of the generator with good stability margin on a system having the unusually high value of x_e of 0.8 per unit.

Figure 4 shows that the low short-circuit-ratio machine when used with a modern amplidyne voltage regulator will have steady-state stability margin even with a system reactance of 0.6 per unit.

In all cases the reactive-ampere lower-limit circuit can now be adjusted with greater confidence to permit the generator to operate at any predetermined range underexcited without exceeding the actual steady-state limit.

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The authors are indebted to F. S. Rothe, S. B. Crary, and J. B. McClure, General Electric Company, for their counsel and to Mrs. D. Stupp and Mrs. M. Dyrkacz for their calculations and assistance.

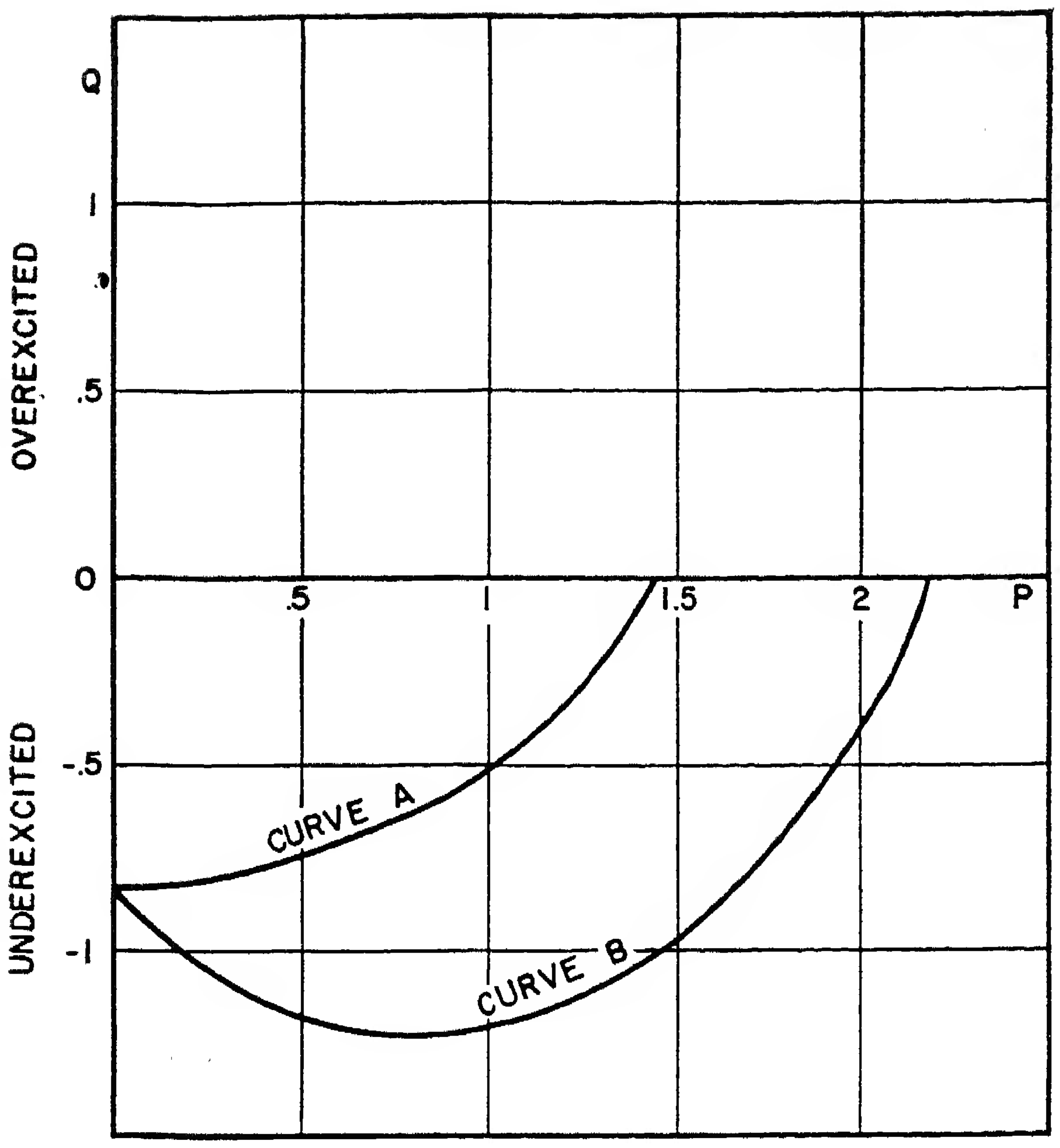

Figure 1 (above). Results of study applying to large turbine generator

Curve A. Steady-state stability limit with noncontinuously acting regulator or close manual control

Curve B. Steady-state limit with continuously acting buck-boost amplidyne regulator

P and Q are real and reactive powers in per unit of maximum turbine rating

$x_d=1.2$, $x_q=1.2$, $x_d'=0.18$, $x_e=0.4$, $T_{d0}'=9$ seconds, $H=5.0$, $D=3.0$, $e_{t0}=1.0$

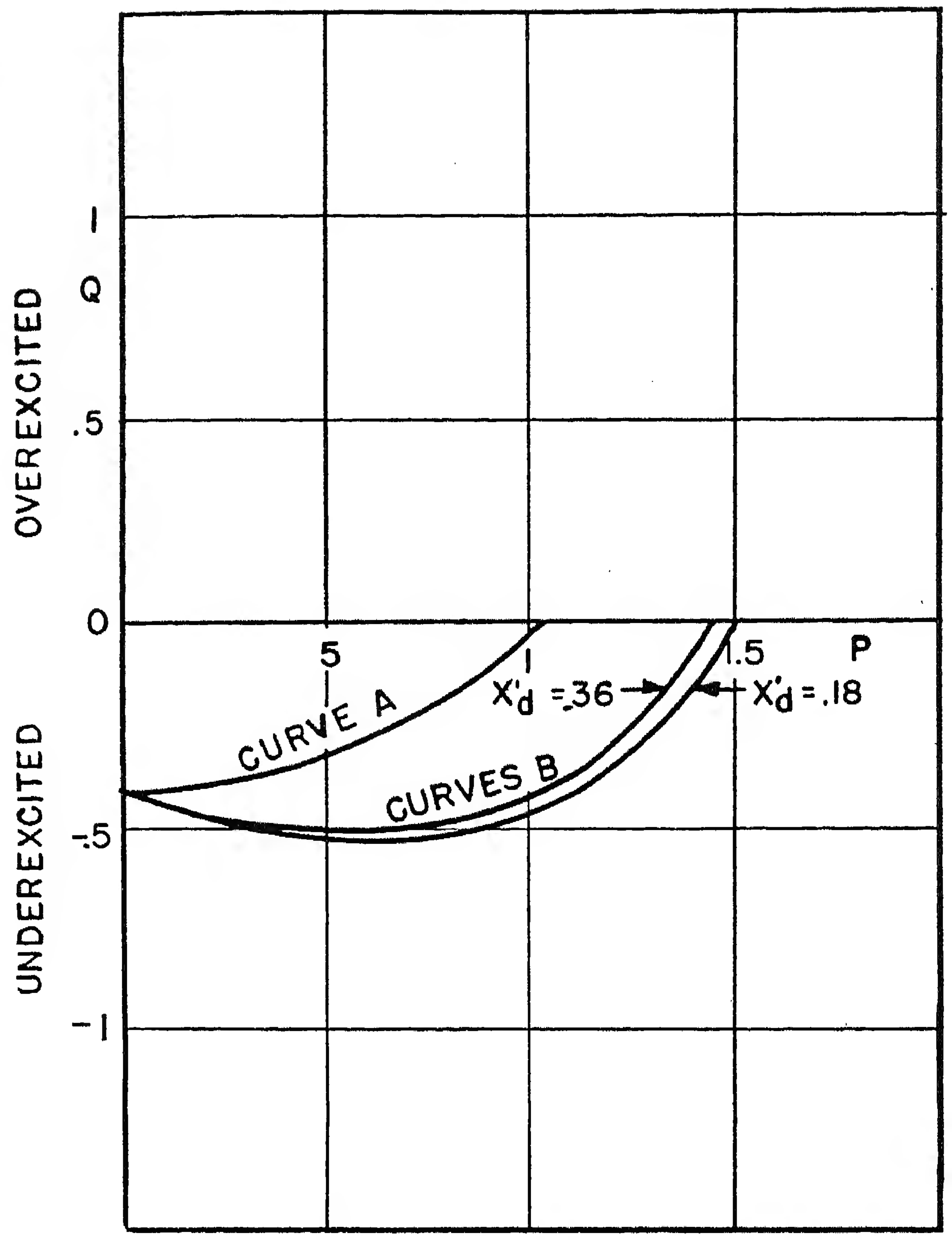

Figure 3 (right). Results of study applying to machine with one-half the short-circuit ratio

Curve A. Steady-state stability limit with noncontinuously acting regulator or close manual control

Curves B. Steady-state limit with continuously acting buck-boost amplidyne regulator

P and Q are real and reactive powers in per unit of maximum turbine rating

$x_d=2.4$, $x_q=2.4$, $x_d'=0.18$ or 0.36, $x_e=0.4$, $T_{d0}'=9$ seconds, $H=5.0$, $D=3.0$, $e_{t0}=1.0$

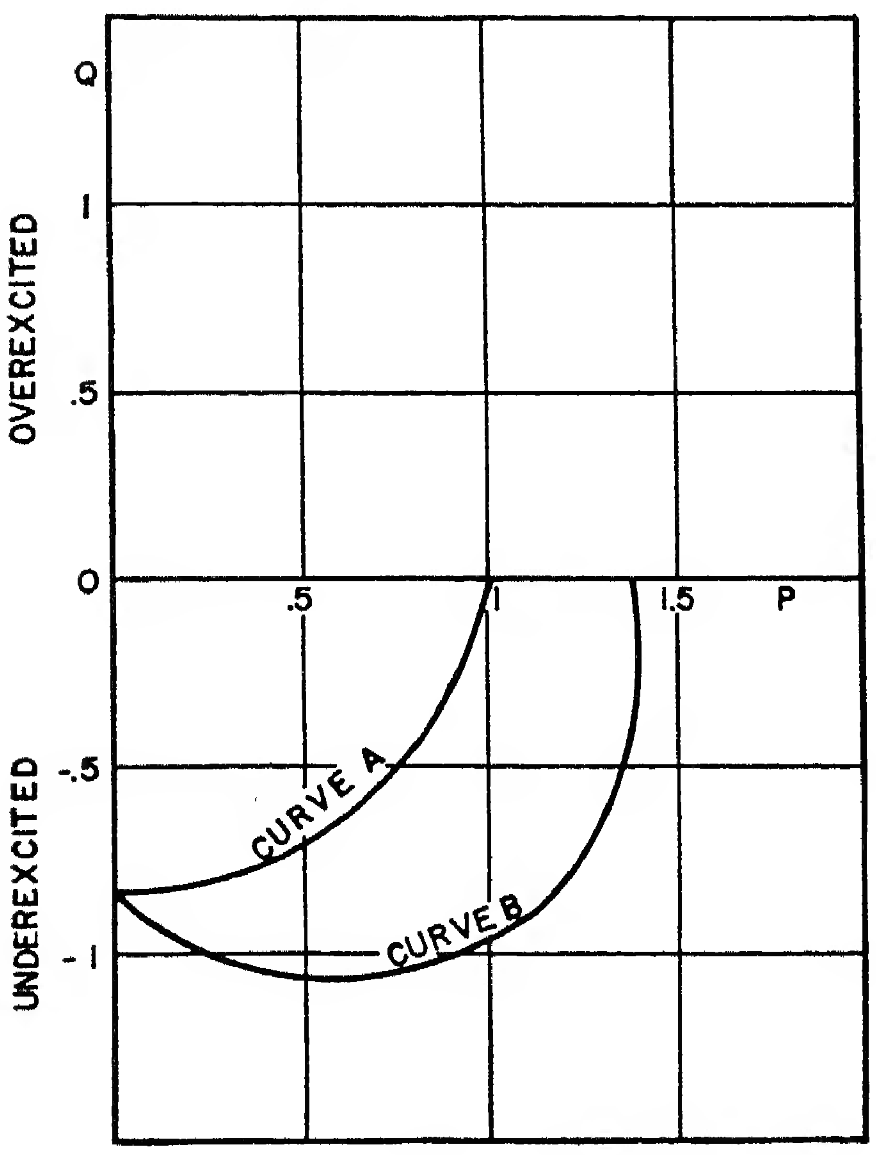

Figure 2. Results of study applying to large turbine generator

Curve A. Steady-state stability limit with noncontinuously acting regulator or close manual control

Curve B. Steady-state limit with continuously acting buck-boost amplidyne regulator

P and Q are real and reactive powers in per unit of maximum turbine rating

$x_d=1.2$, $x_q=1.2$, $x_d'=0.18$, $x_e=0.8$, $T_{d0}'=9$ seconds, $H=5.0$, $D=3.0$, $e_{t0}=1.0$

System Studied

The system studied is shown in Figure 5. This circuit may be considered the simplification of a multigenerator system from the viewpoint of studying the stability performance of only one machine in the system. Thus the external reactance and infinite bus represent the system as seen from the terminals of the machine studied. Means for calculation of x_e are given in a paper by Adams and McClure.[4]

Method of Analysis and Assumptions

The general equations,[5] see Appendix II, describing the performance of the system were first written. In writing these equations, it was assumed that voltages due to rate of change of d and q axis flux linkages and to change of speed are negligible. In addition, the effect of amortisseurs was not considered. In both cases the assumptions are justified because the quantities neglected have negligible effect.[2] Finally, armature and tie-line resistances were neglected. Reasonable amounts have no effect.

A particular operating point was then assigned and the equations rewritten to consider the effect of small changes in currents, voltages, and so forth about their values at this point. The result was a set of linear differential equations. Next, variables were eliminated until the only quantities left were the (change in) terminal voltage, the (change in) machine angle, the (change in) field flux linkages, the (change in) field voltage, and the (changes in) prime mover torque and reference voltage. The block diagram of Figure 6 was then drawn to show schematically these equations.

From this diagram, it may be seen that machine angle has a direct effect ($K_5\Delta\delta$) on terminal voltage as does the magnitude of the field flux ($K_6\Delta\psi_{fd}$). Also it is seen that the torque output of the machine depends upon the machine angle and field flux linkages. The presence of time lags is indicated in the block diagram, wherein it is seen that the machine angle affects the field flux, but only after a delay. Of course, the effects of inertia M and damping D appear in the solution for machine angle. The block diagram is thus an extremely useful tool in understanding the performance of the system. Further, consideration of the system in this manner allows the application of the methods of feedback control system analysis, a powerful tool developed expressly for determination of stability and transient performance.

Practically all of the constants K in the block diagram depend upon the particular operating point assumed in setting up the equations. Thus, to cover the operating region of interest, it was necessary to cal-

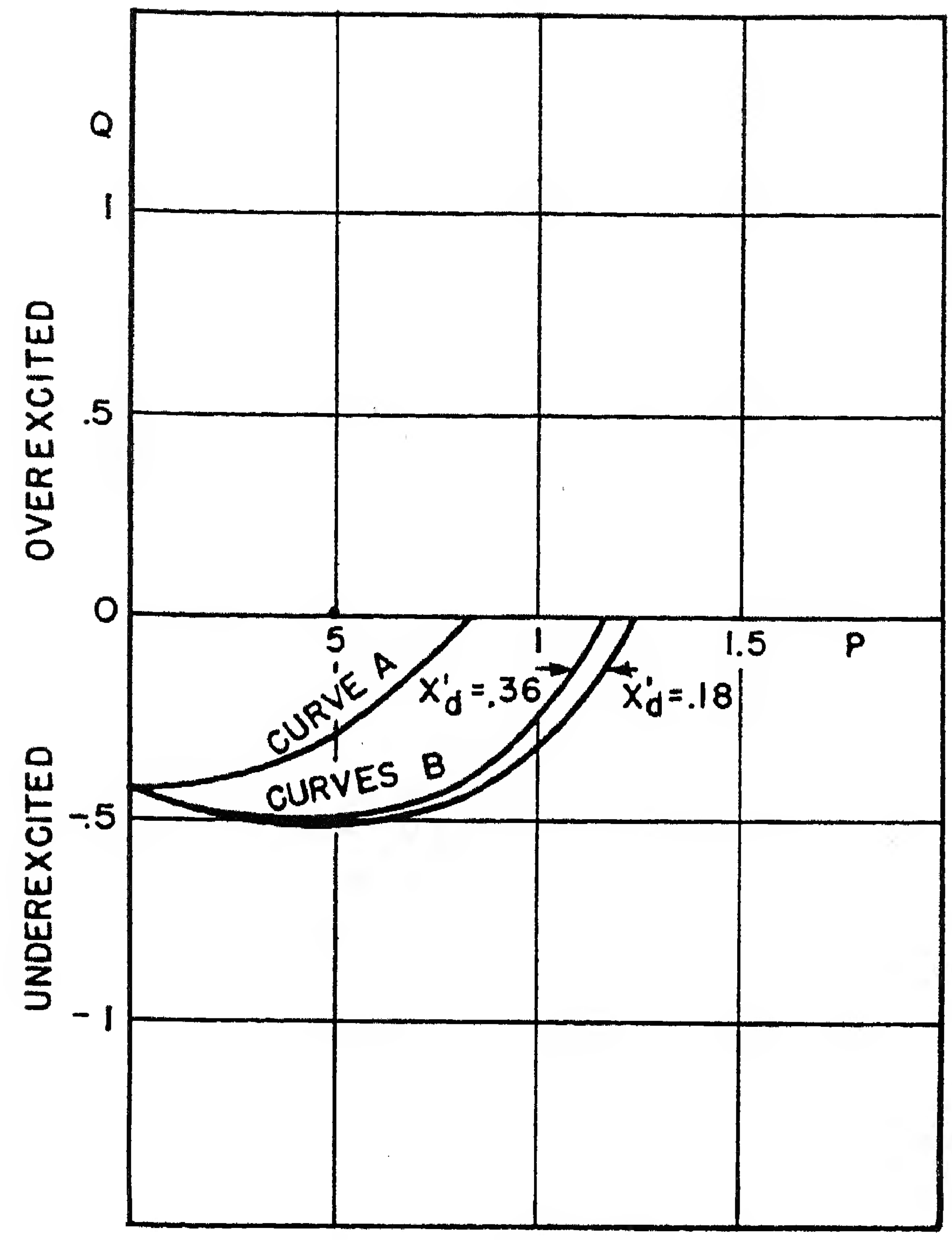

Figure 4 (left). Results of study applying to machine with one-half the short-circuit ratio

Curve A. Steady-state stability limit with noncontinuously acting regulator or close manual control
Curve B. Steady-state limit with continuously acting buck-boost amplidyne regulator
P and Q are real and reactive powers in per unit of maximum turbine rating
$x_d=2.4$, $x_q=2.4$, $x_d'=0.18$ and 0.36, $x_e=0.6$, $T_{d0}'=9$ seconds, $H=5.0$, $D=3.0$, $e_{t0}=1.0$

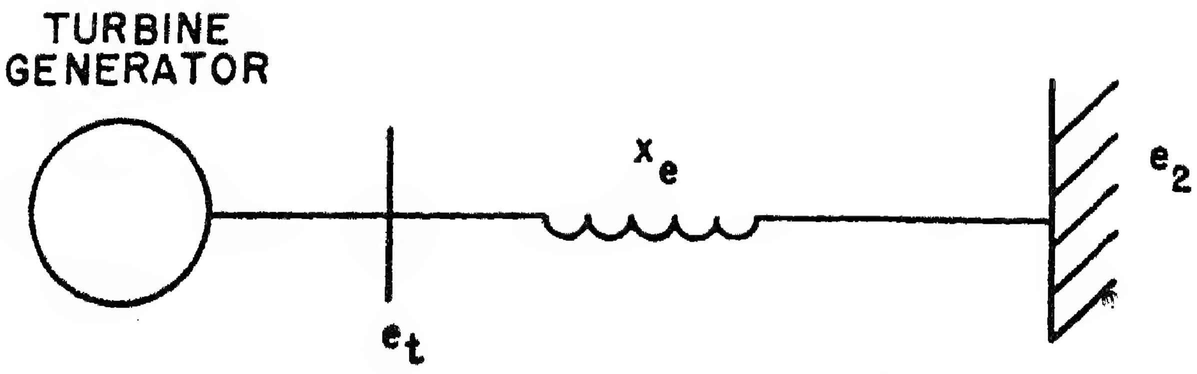

Figure 5. System studied

culate them for a large number of points. However, the equations were put in such a form that the calculations became relatively simple; the use of a modern analogue computer further simplified the calculation of the stability limit so that the study became practical.

Analysis with Manual Control of Exciter Voltage

The performance of the system with the modern regulator cannot be properly evaluated without knowing the performance given by use of manual control or by use of noncontinuously acting types of regulators. For small disturbances about an initial operating point, the dead band inherent in these other type regulators would prevent them from changing the exciter voltage. Tests have shown that even setting the high-speed contacts for very close operation does not appreciably increase the steady-state stability limit.

If the terminal voltage is held constant, the machine power-angle curve is as shown in Figure 7 for the conditions given there. Each point on this curve requires a different amount of exciter voltage. If a test for stability is to be made, it is necessary to calculate the field voltage required, say at points 1, 2, and 3, and then observe the change of power with angle assuming the *field voltage* constant at that particular value. At point 1, for example, the excitation is 1.1 per unit and it is seen that, if the machine accelerates, the power output will increase as the angle increases, tending to reduce the acceleration. This is called stable. At point 2, where E_{fd} is 1.41 per unit, the power output decreases as the machine angle increases beyond this point and the acceleration is increased. Inasmuch as $dP/d\delta=0$ at exactly this point, this is called the stability limit. At point 3, the power output decidedly decreases and the machine is unstable.

Using the block diagram, the same results may be achieved by disconnecting the regulator and holding ΔE_{fd} to zero (no change in exciter voltage). Then the test is made to see if the power output increases as δ increases. The critical point is, of course, the stability limit and is reached when $K_1-K_2K_4=0$ (the time constant T_{dz}' does not affect the limit). Actually a simple equation, see Appendix III, results and the curves of Figure 8 may be drawn. These curves are presented in a nondimensional manner for purposes of generality and do not include the effects of saturation.

Of course, to account for saturation it is possible to compute the equivalent reactance of a machine at a particular operating point.[6] However, pertinent parts of references 4 and 6 may be summarized as follows. In the underexcited region $x_d \cong 1$/short-circuit ratio. In the overexcited region, the equivalent x_d may be 0.6 of nominal x_d at full load or 0.8 of nominal x_d at light loads and thus the

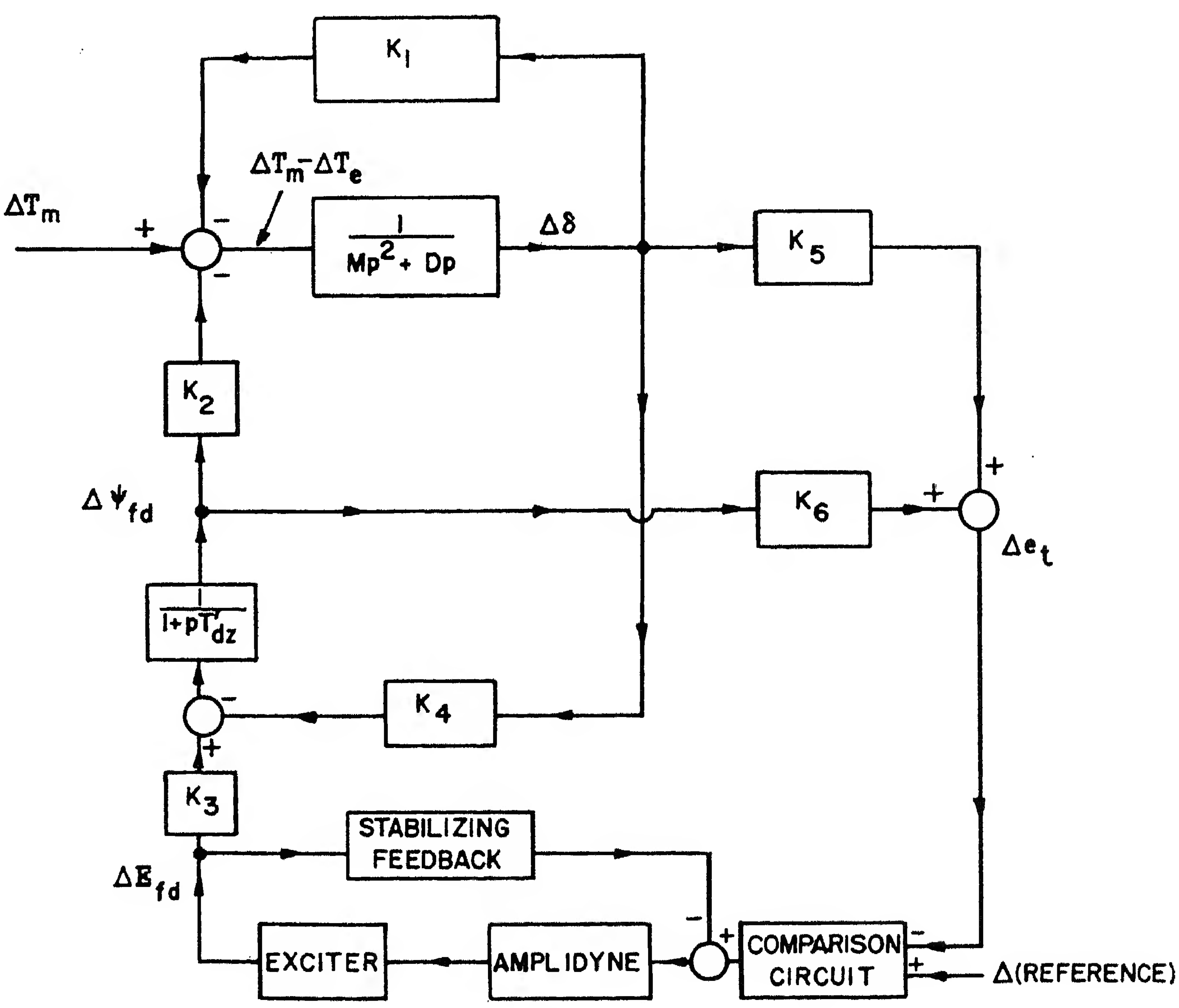

Figure 6. Block diagram of system with continuously acting regulator

steady-state stability limit here is well beyond the operating region.

Analysis with a Modern Continuously Acting Regulator

Inasmuch as the stability limit with a continuously acting buck-boost amplidyne regulator is greater than that achieved with regulators having dead band, it is of interest to describe how the regulator acts to effect this improvement. The history and development of this regulator are discussed by Hunter and Temoshok.[7] The regulator is shown in the block diagram of Figure 6. Briefly, the error between the actual terminal voltage and a reference is amplified by the comparison circuit and by an amplidyne which is connected in series with the exciter field. The rate of change of exciter armature voltage is fed back into the amplidyne through a transformer for stabilization.

Referring to the block diagram, it is seen that the electrical torque is equal to $K_1\Delta\delta+K_2\Delta\psi_{fd}$. Both of these coefficients are positive for all points considered. Now assume that the machine angle increases. As it does the torque output increases ($K_1\Delta\delta$) and the field flux $(-K_4/1+pT_{dz}')\,\Delta\delta$ decreases.

In this region of operation, this decrease in field flux is more than is necessary to offset the increase in electrical torque just mentioned, with the result that the net electrical torque decreases and the machine angle accelerates. But, as the angle increases, the terminal voltage reduces since K_5 is negative for all cases. In response to this change, the regulator increases field voltage, causing an increase in field flux and a resultant increase in electrical torque sufficient to stop the acceleration and start the deceleration necessary to restore the machine angle to its proper value.

Because of the time lags in the regulator and exciter, the regulator may not be able to reduce the excitation to its original value in time to prevent the machine angle from overshooting its proper value as it decelerates. Thus, overshoot may occur and the machine angle may oscillate about its proper value before settling down to a steady state. As the operating condition becomes more severe and K_1 reduces faster than K_2K_4 the regulator must "work harder" to restore the angle. As it does, the tendency to oscillate becomes more predominant. The steady-state limit with a continuously acting regulator is that point beyond which there is a sustained oscillation of increasing amplitude.

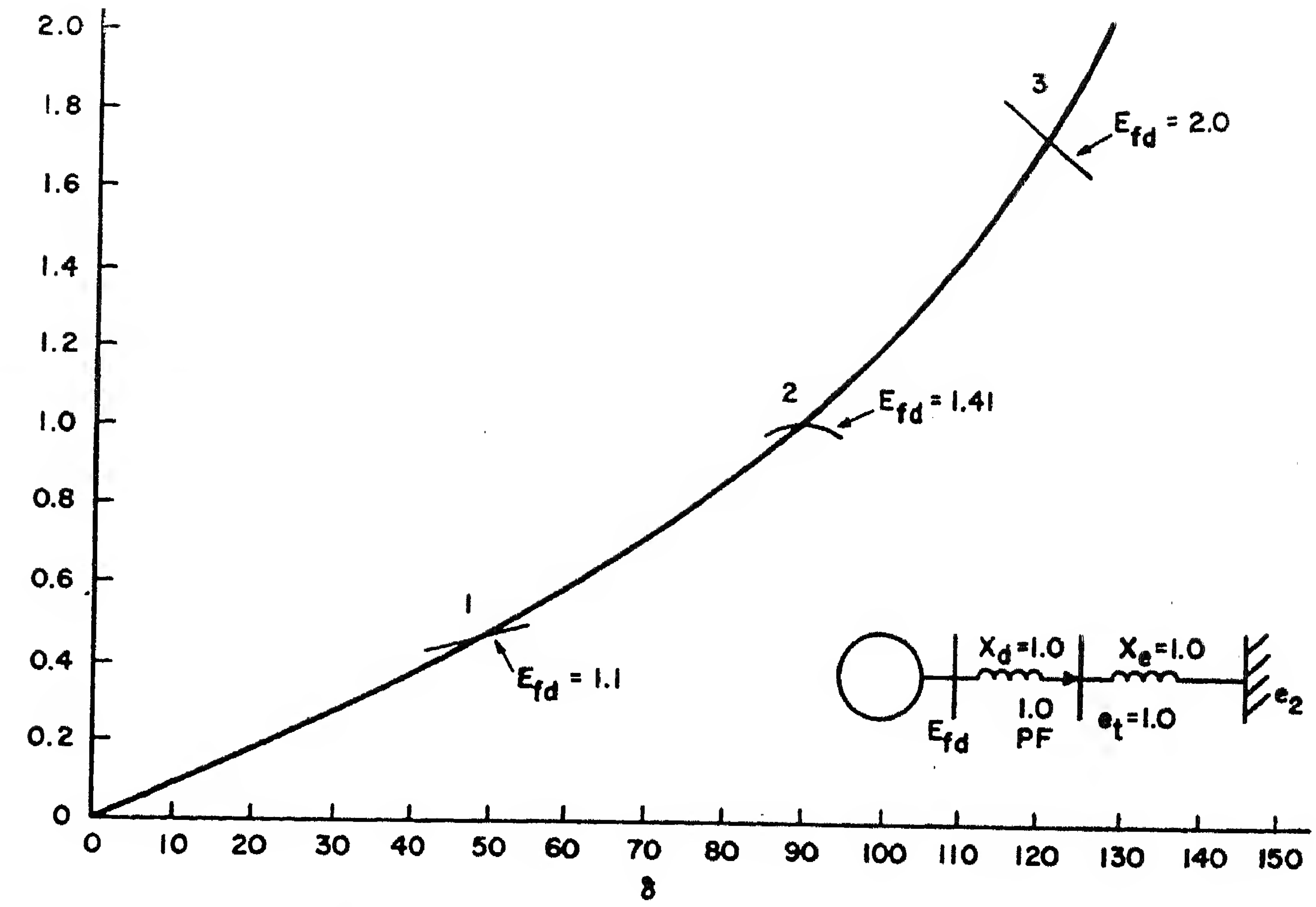

Figure 7. Power angle characteristics of system studied. With constant exciter voltage, point 1 is stable, point 2 is the stability limit, and point 3 is unstable

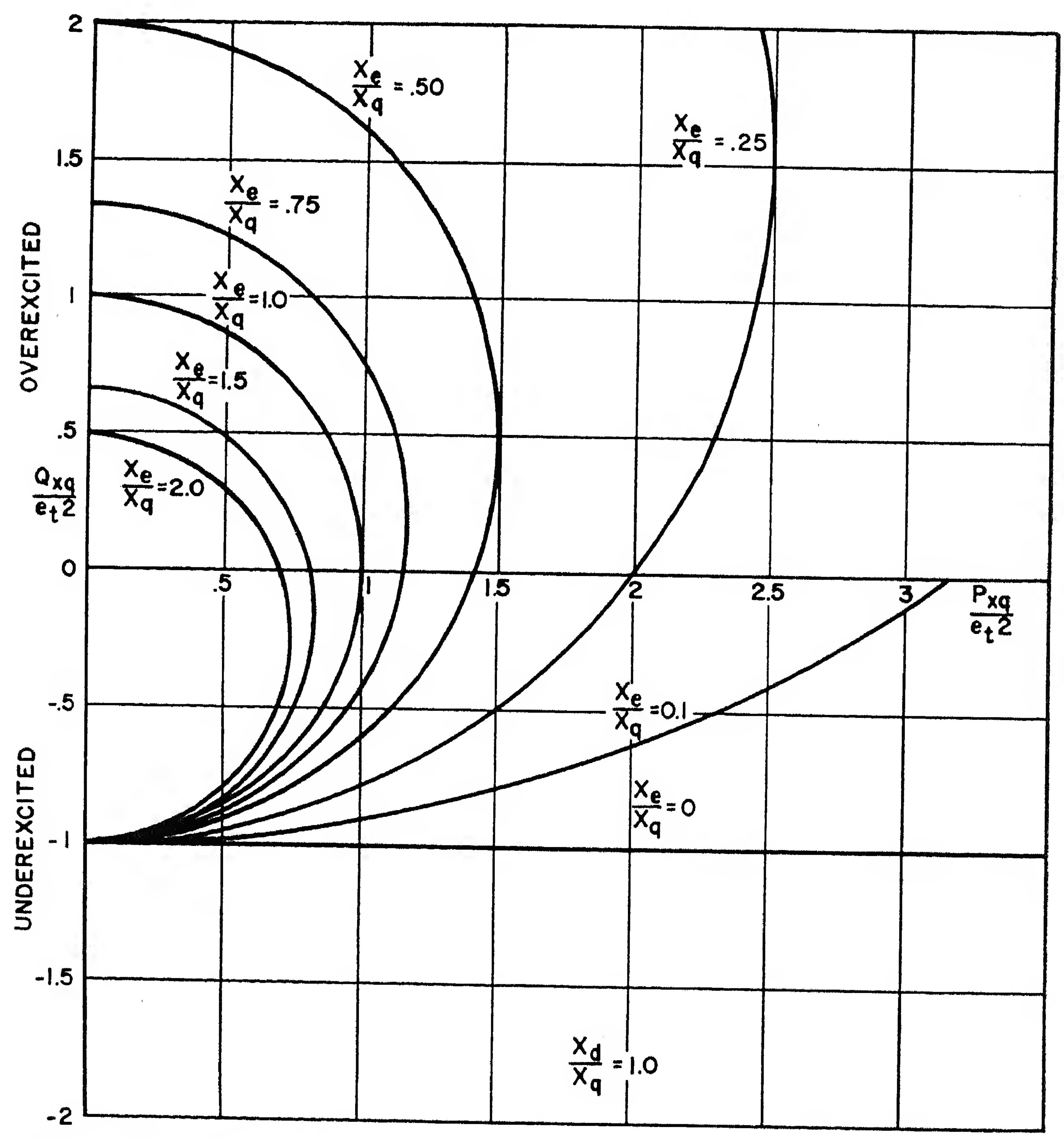

Figure 8. Generalized steady-state stability limit curves for noncontinuously acting regulator or manual control. Saturation is not considered

In obtaining the results of this study, the equations represented by the block diagram were set up on an analogue computer. The coefficients of the equations were set on the computer for a particular combination of real and reactive power. A step input of reference voltage was introduced and the time responses of ΔE_{fd}, $\Delta\psi_{fd}$, Δe_t, and $\Delta\delta$ were recorded. If the resulting oscillation in the various

quantities damped out, the condition was stable and an initial operating point at slightly higher kilovolt-amperes was tried. When a stable point and an unstable point separated by not more than 0.05 per unit kilovolt-ampere were found, a point on the stability limit locus was plotted between them. Thus, it follows that the accuracy is within 0.05 per unit kilovolt-ampere. A number of check points were calculated using Nyquist's theorem of stability[8] and no measurable difference from computer results was found.

Appendix I. Nomenclature

e_d = direct-axis voltage
e_q = quadrature-axis voltage
e_t = terminal voltage
E_{fd} = field voltage
e_2 = voltage of infinite bus
ψ_d = direct-axis flux linkages
ψ_q = quadrature-axis flux linkages
ψ_{fd} = field flux linkages
x_d = direct-axis synchronous reactance
x_q = quadrature-axis synchronous reactance
x_d' = direct-axis transient reactance
x_e = equivalent system reactance
i_d = direct-axis current
i_q = quadrature-axis current
I_{fd} = field current in per unit of current required to produce rated voltage at no load, synchronous speed
T_{d0}' = open-circuit generator field time constant
δ = angle between quadrature axis and the infinite bus voltage
θ = angular position of direct axis with respect to stator
$M = 4\pi fH$ where H is per-unit inertia constant
D = prime mover damping in per unit
T_m = prime mover torque
T_e = electrical torque
p = time derivative operator d/dt
P = real power
Q = reactive power
i_P = real component of current
i_Q = reactive component of current
The subscript zero denotes an initial operating condition.
Δ quantities denote small excursions about an initial operating point.
Time is measured in radians.

Appendix II

The equations describing the system of Figure 5, for a machine with no amortisseurs and with negligible resistances, are

$$e_d = p\psi_d - \psi_q p\theta \tag{1}$$

$$e_q = p\psi_q + \psi_d p\theta \tag{2}$$

$$e_d = px_e i_d - x_e i_q p\theta + e_2 \sin\delta \tag{3}$$

$$e_q = px_e i_q + x_e i_d p\theta + e_2 \cos\delta \tag{4}$$

$$\psi_d = I_{fd} - x_d i_d \tag{5}$$

$$\psi_q = -x_q i_q \tag{6}$$

$$Mp^2\theta = T_m - \psi_d i_q + \psi_q i_d - D(p\theta - 1) \tag{7}$$

$$\psi_{fd} = I_{fd} - (x_d - x_d')i_d \tag{8}$$

$$E_{fd} = I_{fd} + T_{d0}' p\psi_{fd} \tag{9}$$

$$e_t^2 = e_d^2 + e_q^2 \tag{10}$$

Writing $\psi_d = \psi_{d0} + \Delta\psi_d$, and so forth, and noting that

$$e_2 = e_{20} \tag{11}$$

$$p\theta = 1 + p(\Delta\theta) = 1 + p(\Delta\delta) \tag{12}$$

$$\sin\Delta\delta = \Delta\delta \tag{13}$$

$$\cos\Delta\delta = 1 \tag{14}$$

the equations reduce to those given below.

$$\Delta e_d = [p\Delta\psi_d] - \Delta\psi_q - [\psi_{q0} p\Delta\delta] \tag{15}$$

$$\Delta e_q = [p\Delta\psi_q] + \Delta\psi_d + [\psi_{d0} p\Delta\delta] \tag{16}$$

$$\Delta e_d = [x_e p\Delta i_d] - x_e\Delta i_q + (e_{20}\cos\delta_0)\Delta\delta - [i_{q0} x_e p\Delta\delta] \tag{17}$$

$$\Delta e_q = [x_e p\Delta i_q] + x_e\Delta i_d - (e_{20}\sin\delta_0)\Delta\delta + [i_{d0} x_e p\Delta\delta] \tag{18}$$

$$\Delta\psi_d = \Delta I_{fd} - x_d\Delta i_d \tag{19}$$

$$\Delta\psi_q = -x_q\Delta i_q \tag{20}$$

$$(Mp^2 + Dp)\Delta\delta = \Delta T_m - \psi_{d0}\Delta i_q - i_{q0}\Delta\psi_d + \psi_{q0}\Delta i_d + i_{d0}\Delta\psi_q \tag{21}$$

$$\Delta\psi_{fd} = \Delta I_{fd} - (x_d - x_d')\Delta i_d \tag{22}$$

$$\Delta E_{fd} = \Delta I_{fd} + T_{d0}' p\Delta\psi_{fd} \tag{23}$$

$$\Delta e_t = \frac{e_{d0}}{e_{t0}}\Delta e_d + \frac{e_{q0}}{e_{t0}}\Delta e_q \tag{24}$$

The eight terms in brackets are neglected. Next, all variables except Δe_t, $\Delta\delta$, $\Delta\psi_{fd}$, ΔT_m, and ΔE_{fd} are eliminated leaving three equations in three unknowns, inasmuch as ΔT_m and ΔE_{fd} are independent variables. The following additional relationships are used in obtaining the result

$$e_{d0} + x_e i_{q0} = e_{20}\sin\delta_0 \tag{25}$$

$$e_{q0} + x_q i_{d0} = E_{q0} \tag{26}$$

$$e_{q0} - x_e i_{d0} = e_{20}\cos\delta_0 \tag{27}$$

$$T_{dz}' = \frac{x_d' + x_e}{x_d + x_e} T_{d0}' \tag{28}$$

The final equations are

$$(Mp^2 + Dp)\Delta\delta = \Delta T_m - \frac{e_{20}\sin\delta_0}{x_d' + x_e}\Delta\psi_{fd} - \left[\frac{E_{q0}e_{20}\cos\delta_0}{x_e + x_q} + \frac{e_{d0}e_{20}\sin\delta_0}{x_d' + x_e}\times\left(1 - \frac{x_d'}{x_q}\right)\right]\Delta\delta \tag{29}$$

$$\Delta\psi_{fd} = \frac{x_d' + x_e}{x_d + x_e}\,\frac{1}{1 + pT_{dz}'}\Delta E_{fd} - \frac{x_d - x_d'}{x_d + x_e}e_{20}\sin\delta_0\frac{1}{1 + pT_{dz}'}\Delta\delta \tag{30}$$

$$\Delta e_t = \frac{e_{q0}}{e_{t0}}\,\frac{x_e}{x_d' + x_e}\Delta\psi_{fd} + \left[\frac{e_{d0}}{e_{t0}}e_{20}\cos\delta_0\frac{x_q}{x_e + x_q} - \frac{e_{q0}}{e_{t0}}\times e_{20}\sin\delta_0\frac{x_d'}{x_d' + x_e}\right]\Delta\delta \tag{31}$$

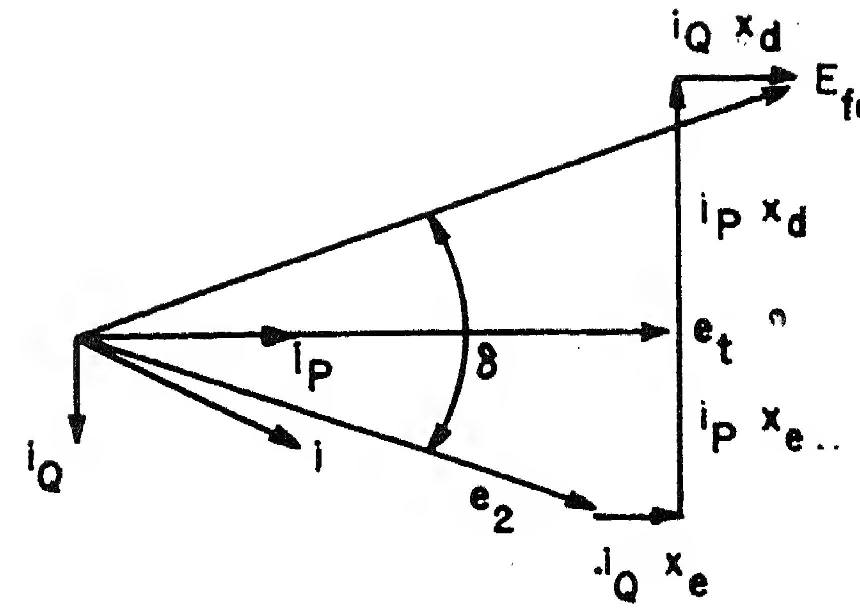

Figure 9. Machine steady-state vector diagram

Appendix III. Stability Limit with Constant Exciter Voltage

Referring to Figure 9, the following relationships may be derived

$$P = \frac{E_{fd}e_2}{x_d + x_e}\sin\delta \tag{32}$$

$$Q = \frac{E_{fd}^2 x_e}{(x_d + x_e)^2} - E_{fd}e_2\cos\delta\left[\frac{x_e - x_d}{(x_d + x_e)^2}\right] - \frac{e_2^2 x_d}{(x_e + x_d)^2} \tag{33}$$

$$P = e_t i_P \tag{34}$$

$$Q = e_t i_Q \tag{35}$$

$$E_{fd}^2 = (e_t + i_Q x_d)^2 + (i_P x_d)^2 \tag{36}$$

$$e_2^2 = (e_t - i_Q x_e)^2 + (i_P x_e)^2 \tag{37}$$

$$\frac{dP}{d\delta} = \frac{E_{fd}e_2}{x_d + x_e}\cos\delta = \frac{E_{fd}^2 x_e}{(x_d + x_e)(x_d - x_e)} + \frac{e_2^2 x_d}{(x_d + x_e)(x_d - x_e)} + Q\frac{x_d + x_e}{x_d - x_e} \tag{38}$$

The stability limit occurs at $dP/d\delta = 0$.

$$Q = \frac{E_{fd}^2 x_e}{(x_d + x_e)^2} - \frac{e_2^2 x_d}{(x_d + x_e)^2} \tag{39}$$

This is true when

$$\left(\frac{Px_d}{e_t^2}\right)^2 + \left[\frac{Qx_d}{e_t^2} + \frac{1}{2}\left(1 - \frac{x_d}{x_e}\right)\right]^2 = \frac{1}{4}\left(1 + \frac{x_d}{x_e}\right)^2 \tag{40}$$

This equation describes a circle and it follows that points inside the circle are stable whereas points outside the circle are unstable.

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Discussion

C. Concordia (General Electric Company, Schenectady, N. Y.): This paper constitutes a valuable contribution to the literature of generator voltage regulators from at least two standpoints.

First, it presents data on the improvement in the steady-state power limit resulting from use of an actual, available regulator, as distinguished from previous papers[1] which merely showed that by proper regulator design it was possible to achieve this improvement. By presenting these data, the paper answers questions which were raised by the discussors of reference 3 of the present paper.

It should be pointed out that the primary function of the regulator studied in the paper is to control voltage properly, not to improve system stability. Thus the improvement in stability obtained is to be regarded as an additional and incidental benefit accruing from the use of modern voltage regulators. Moreover, it should be evident that if improvement in stability rather than voltage regulation had been the primary design objective, an even greater stable region would have resulted.

Second, it presents data over the whole region of underexcited operation, the previous papers having been confined principally to the unity power-factor region.

Finally, we should like to comment on item 2 of the section entitled "Conclusions" which states that the use of a modern voltage regulator permits a reduction in short-circuit ratio. This should not be taken to imply that a modern continuously acting voltage regulator will always be necessary if any further reductions in short-circuit ratio are made. On the contrary, in most applications it will be found that short-circuit ratio is not a limitation, provided that practically any well-designed voltage regulator is used.

References

1. See references 2 and 3 of the paper.

V. V. Mason (Hydro-Electric Power Commission of Ontario, Toronto, Ontario, Canada): The authors do not describe the tests which they claim showed that the dead-band type of voltage regulator gives no increase in the stability limit over that obtainable on fixed excitation. Tests made by The Hydro-Electric Power Commission of Ontario on a large hydraulic generator connected to a system via a long transmission line ($x_e = 0.3$) showed a very definite increase in the stability limit with a contactor-type, dead-band regulator over the limit for constant excitation. These tests were done at constant power by reducing the regulated voltage in steps until pull-out was incipient. As a result, the data so obtained are not directly comparable with those presented by the authors but extrapolation of the curves indicates the same order of benefit in terms of pull-out power as shown for an amplidyne regulator.

It would seem that the most reasonable explanation for this difference between theory and practice is the fact that pull-out is actually a slow process owing to the time constants and damper windings necessarily neglected in the simplified theory. These give the regulator time to increase the excitation and prevent pull-out even though operating at a point where the constant-excitation torque-angle curve has a negative slope. This effect was visible in high-speed, suppressed-zero, graphic voltmeter charts taken during the tests. These charts showed a sawtooth voltage wave of small amplitude which increased in magnitude as the stability limit was approached.

Alexander Dovjikov (Bonneville Power Administration, Chicago, Ill.): It is to be hoped that the authors will extend their analysis further into the "transient stability" region. After all, the "proof of the pudding" is the transient stability, although measures which improve steady state will act favorably during transient conditions also.

The point that attracted my attention was the statement that machines supplied with amplidyne excitation may be designed with lower short-circuit ratio. Is that true for transient stability as well? Is it possible to decrease the short-circuit ratio of a synchronous machine without increasing its transient reactance, the low value of which is so important for transient stability of long-distance transmission? During the light-load conditions the operation of synchronous generators at leading power factor may be necessary but to what extent is it desirable (as assumed by the authors in the first paragraph of the paper)? With the modern, high-speed excitation systems and continuously acting automatic voltage regulators, underexcited operation may be more feasible than before, but considering transient stability, the maintenance of which is particularly important when the system is fully loaded, the advisability of generator design with lower short-circuit ratio, even if such generators are less expensive, is questioned.

Impulse Testing of Power Transformers

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THE first full-sized power transformer was impulse-tested in 1930,[1] and in 1933[2] commercial impulse testing was started in accordance with recommendation formulated by the Transformer Subcommittee of the AIEE Electrical Machinery Committee. Since then the authors have been closely associated with the testing of over 1,100 power transformers following AIEE or American Standards Association (ASA) rules. Gradually the value of impulse testing has been accepted until today a large percentage of all large high-voltage transformers receive the test.

Originally the principal deterent to impulse testing was the fear that undetected damage would be inflicted on the transformer.[3,4] This has led to the development of highly refined methods of failure detection.[5] If these methods are used, there is little chance of an undetected impulse failure reducing the service life of a transformer. This problem having been solved, the next objective should be the introduction of a simpler test.

With the trend toward reducing transformer insulation levels,[6] it becomes more necessary to avoid inflicting undetected damage during an impulse test. Also, more attention must be given to switching surge stresses.

Service Record

The substantial improvement in the impulse strength of transformers in the past 2 decades, coupled with advances in lightning arresters and general protective

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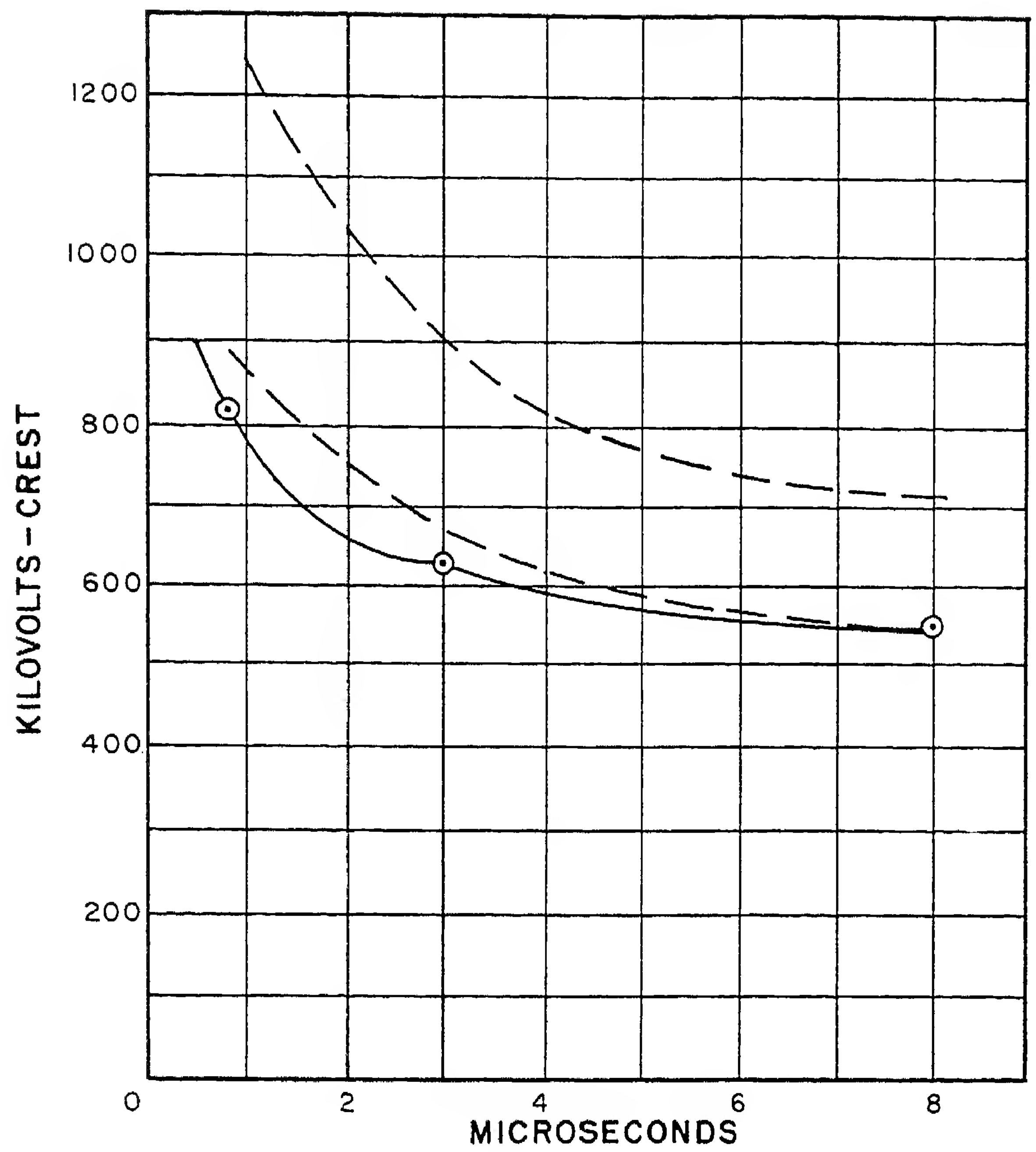
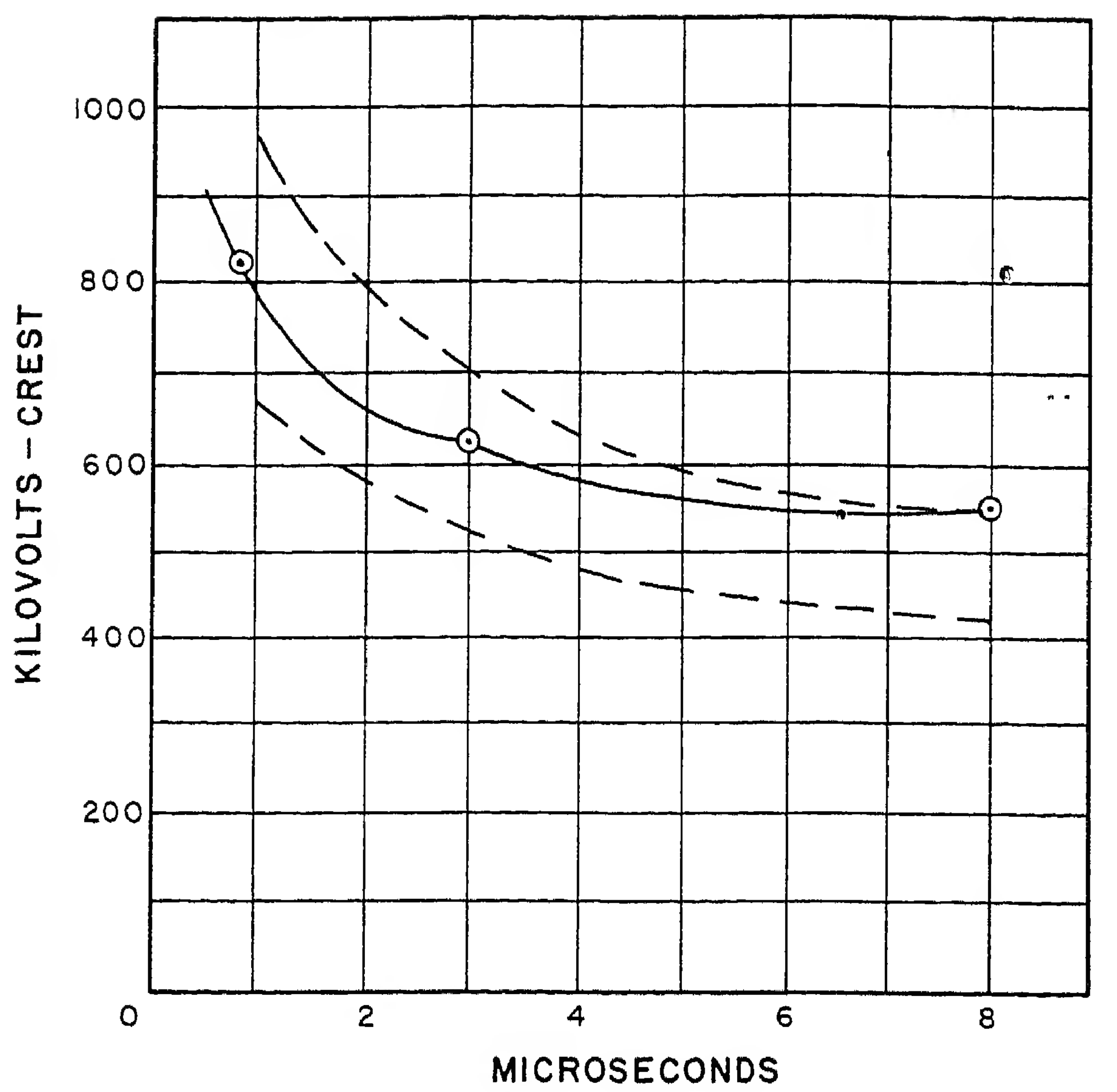


Area between dashed curves is the volt-time area of spark-over for a 31-inch rod gap

Figure 1 (left). Comparison between transformer impulse test withstand levels for 115-kv insulation class and protection offered by a 31-inch rod gap

Figure 2 (right). Comparison between transformer impulse test withstand levels for 115-kv insulation class and protection offered by a 24-inch rod gap

Points on solid line—transformer impulse tests according to ASA and NEMA



Area between dashed curves is the volt-time area of spark-over for a 24-inch rod gap

practices, has practically eliminated lightning failures of power transformers in service. This has led to widespread acceptance of the "reduced insulation" philosophy on grounded neutral systems and to a general review of the prescribed dielectric tests on transformers, both low frequency and impulse.

In studying statistics on service failures of transformers built during the last 20 years, several interesting facts are apparent:

1. The failures occurring in service are not of the same type as the failures during impulse test.

2. Lightning failures are very rare where reasonable protective measures are taken.

3. There is a negligible difference in service life between those transformers which have received impulse test and those that have not.

As a direct result of impulse testing, certain types of service failures no longer occur because the lessons learned from the impulse-tested units have been applied to all transformers.

The comparison between service and impulse-test failures justifies the policy of limiting impulse testing to the number of transformers that can be tested carefully, and with the best available failure detection techniques.

Expansion of Impulse Testing

Historical Background

When impulse testing was being established, the type of test to be applied was based on the assumption that the bushing would act as a co-ordinating device. Consequently, the test called for a number of impulse waves with amplitudes below bushing flashover followed by two impulse waves of sufficient magnitude to flashover the bushing. Later the bushing flashover was changed into the requirement of sparking over a standard rod gap which could be duplicated most readily in all laboratories. Thus, the chopped-wave and full-wave tests were born. This test was refined in 1937 when the impulse crest to be applied was given in terms of voltage rather than gap spacing. However, these voltages were largely based on the spark-over characteristics of the previously standardized "test gaps."

Another development occurred when it was discovered that the co-ordinating air gaps did not protect transformers in the field. The gap spacings used on a 110-kv system were from 24 inches to 31 inches. Figures 1 and 2 show the volt-time spark-over areas for these gaps superimposed on the present-day ASA and National Electrical Manufacturers Association (NEMA) impulse withstand curves for transformers. Remembering that the transformers which failed were built in the 1920's and early 1930's without the present knowledge of impulse stresses in the windings, it is quite evident that it did not take steep front waves to damage the windings but that failure probably occurred from ordinary chopped and full waves permitted by these gaps. The literature[7] shows that this particular system then determined the gap spacing that could be tolerated from a line outage point of view and then considered such a gap as a protective device for the transformer. To prove that the transformer could be protected by such a gap, a new test was introduced the front of wave test requiring spark-over of a rod gap with a specified spacing by a wave rising at the rate of 1,000 kv per microsecond.

Types of Test

There are essentially two systems of station equipment used in this country. The predominant one is the use of lightning arresters in the station. The other one is the use of some kind of spark gap, usually a form of rod gap. This latter form of protection has a very wide range of spark-over and also results in extremely rapid changes of potential when the gap sparks. The lightning arrester protection is of the valve type which does not permit such rapid changes of potential at the apparatus terminal and provides a more predictable protective level. The voltages applied to a transformer protected by a lightning arrester are more nearly of the type of a full wave. However, if the lightning arrester is placed some distance from a transformer, the circuit constants will permit voltages at the transformer terminals in excess of those at the arrester terminals. These are of short duration but do not represent waves equivalent to the present test requirements.

For these reasons a completely realistic type of impulse test on transformers would, first of all, require a full wave which is the type of wave most frequently encountered in service. To take care of the possibility of a spark-over of station insulation within the station, a chopped wave at the same level as the full wave

would be applied. For the rare case of traveling waves of high amplitudes with steep fronts reaching the station, a test would be required with steep front and moderately steep tail. From circuit analysis the tail of the wave in a station would not be steeper than the front. The amplitude of such a test wave can be determined with reasonable accuracy from tests in the laboratory simulating service conditions. Tests to date indicate that it would be appreciably less than the present NEMA front of wave test.

With these more realistic test requirements it is believed that transformer designers eventually could effect some economies for the benefit of the industry, and for this reason further discussion of these tests is in order.

Increasing Number of Transformers Tested

A substantial increase in impulse testing now, or in the near future, can be accomplished only by simplifying the test. Several alternative procedures have been studied.

It becomes increasingly apparent that the full-wave test should be the basic transformer test. Experience has shown this test to be a very effective means of proving manufacturing and design quality. The application of a reduced (50 per-cent) full wave, followed by two full waves at basic impulse insulation level, consumes a minimum of time and permits full use of the neutral current method of failure detection.

Trial use of this test on selected transformers has been started to determine its effectiveness; and studies are in progress, in accordance with established quality control principles, to determine the number of transformers that can be tested economically in this manner.

Complete ASA and NEMA impulse tests will, of course, continue to be made when specified.

Impulse Failure Detection

What is a Failure?

In the past there have been many definitions of what constitutes a failure. These range from the indisputable service experience, where failure is discovered only when low-frequency follow current is established forcing the opening of a circuit breaker to disconnect the transformer from the line, to the highly refined impulse-testing methods of comparing oscillograms of line voltage and ground current obtained at the basic impulse insulation test level and at a much reduced voltage.

It is an observed fact that some failures produced during an impulse test, particularly those produced by chopped waves, will seal off rather quickly, some in a matter of minutes, others in the course of several hours, so that a full-wave test can be passed without any sign of failure. The chances that such failures will recur in service are small and the risk taken in installing such transformers, most of which are used in a well-protected system, is also small.

Therefore, it is rather difficult to agree on a practical definition for an impulse-test failure that will reduce service life, and this is probably the principal difference of opinion among the various groups interested in failure detection.

The authors have used the following as a basis for failure detection practice:

1. A failure is defined as any breakdown of insulation—solid, oil, or air—between two metal electrodes in a transformer which short-circuits part or all of the winding regardless of the extent of the short circuit.

2. A short circuit which involves principally a puncture of solid insulation in a confined location is a failure which is most likely to result in follow current, if reproduced in the field, because the gases produced by the spark are confined and cannot readily escape. Also, carbonization of the solid insulation may result in greatly reduced low-frequency strength.

3. To test failures for this condition, a program was arranged to investigate the voltage level at which failures would seal off, compared to the basic impulse insulation full-wave level. If the seal-off or critical level is low, it must be due to greatly reduced insulation strength and such cases are considered particularly serious.

The definition and test procedure are a practical approach to the problem because they take account of service life and because each failure has been found in dismantling the unit.

How Are Failures Detected?

Here again there is considerable disagreement which is greatly influenced by the definition of a failure. The authors recognized early that the oscillograph record constitutes the best over-all evaluation of the condition of the transformer after the test is completed. This technique has been described previously[5] but briefly consists of obtaining oscillograph records in three ways:

1. An impulse-voltage wave applied to the transformer is recorded at a level of about 50 per cent of the full wave required by the BIL and later compared with the full-wave oscillogram.

2. At both levels, oscillograms of the current in the grounded end of the impulsed winding are obtained and compared.

3. Steep front and chopped waves, where applied, also are examined carefully for possible wave-shape changes which may indicate a failure.

If the wave shape of the voltage waves and the current waves at both levels agree, the transformer has passed the test. If there are discrepancies in the wave shape and impulse circuit troubles have been eliminated as the cause, the transformer is considered to have failed. The transformer is inspected for oil bubbles, smoke, or carbonized oil, and the noise produced in the tank is observed. The impulse voltage is lowered and the critical failure level is established as the voltage at which the failure no longer occurs. In those cases where oscillographic changes and bubbles, smoke, or carbonization are observed, or an unusual noise exists in the transformer, the decision of declaring a

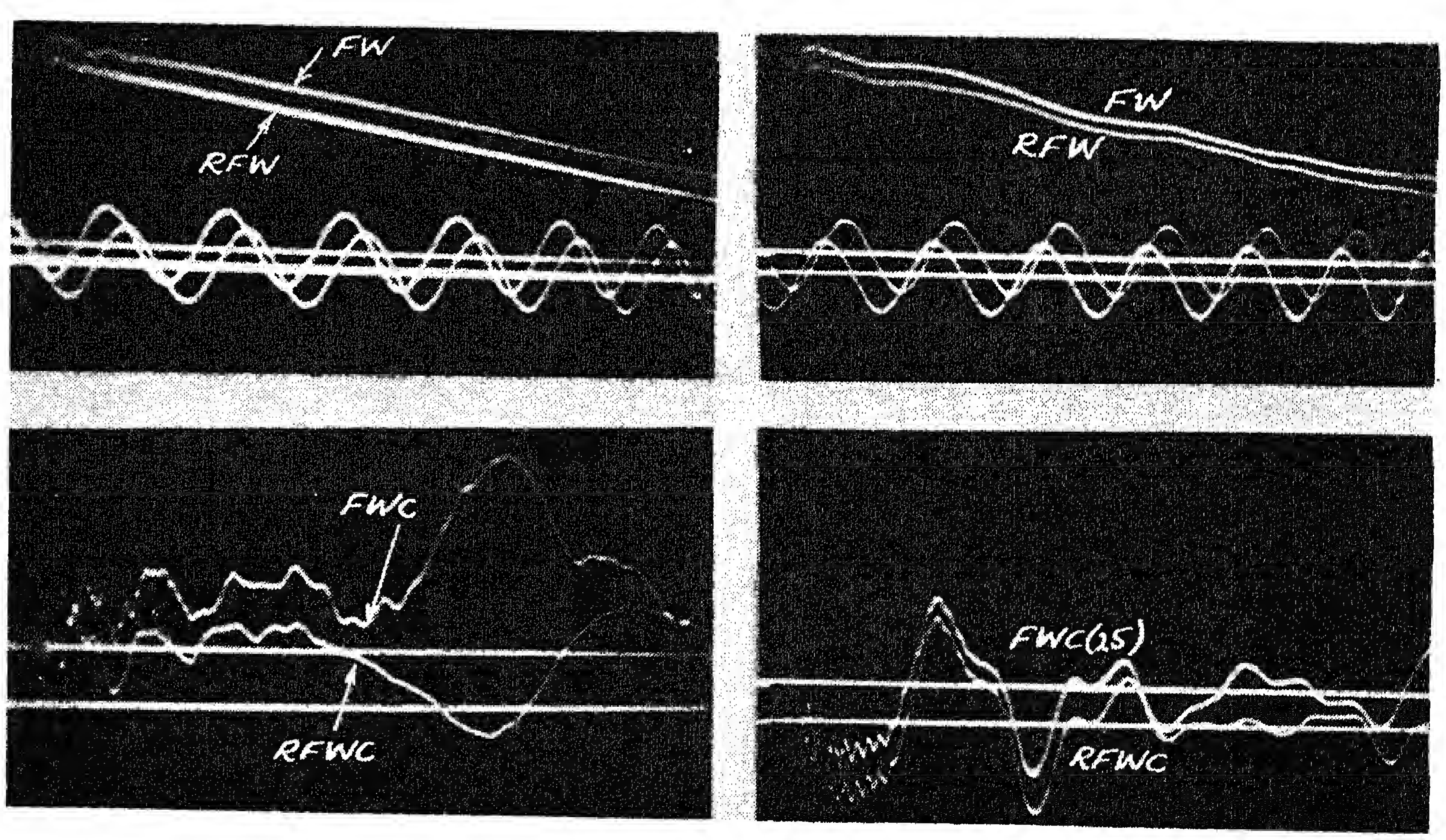


Figure 3. Voltage and current oscillograms indicate failure. Oscillograms at top show voltage waves. Oscillograms on bottom show current waves

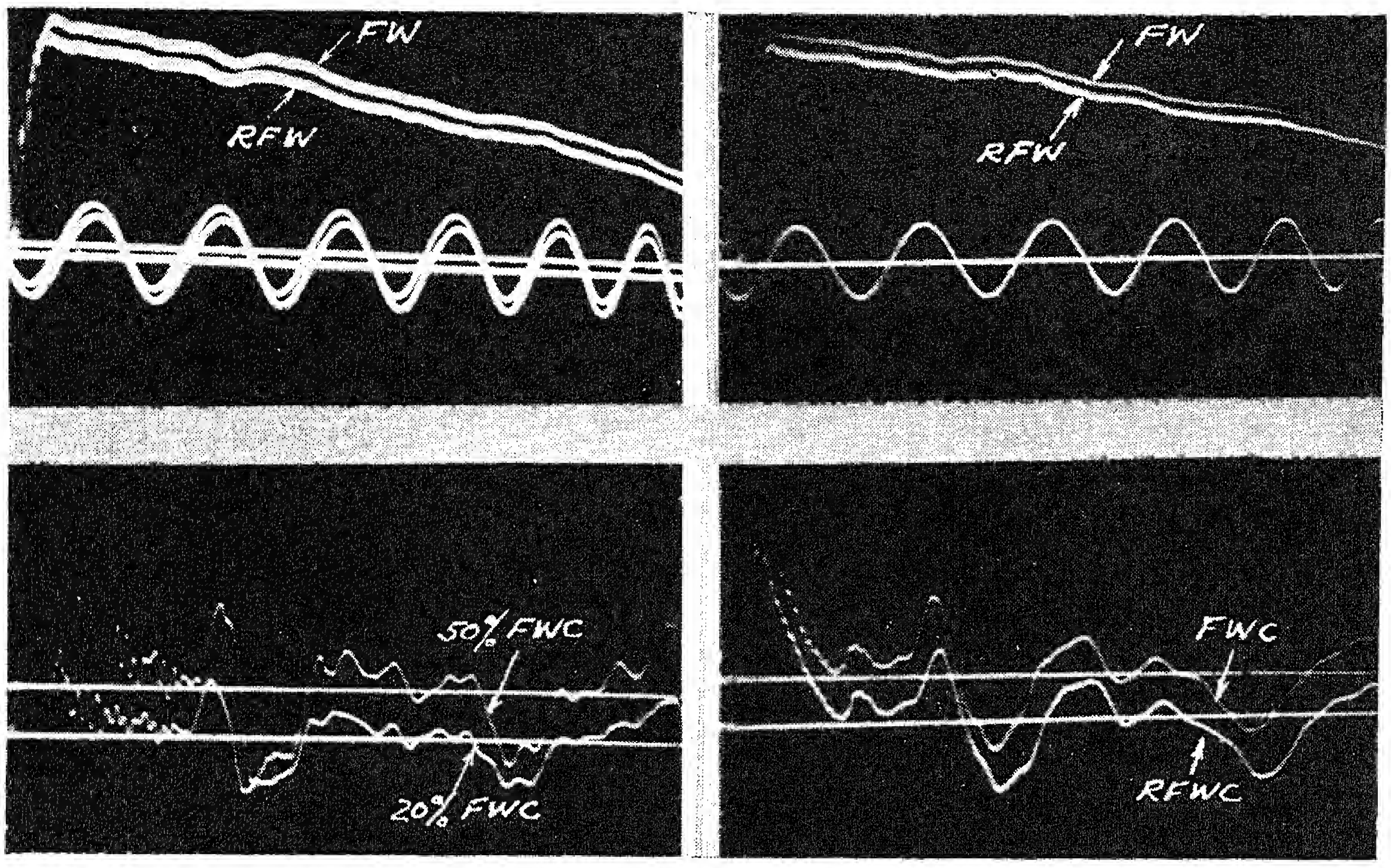


Figure 4. Current oscillograms indicate failure. Oscillograms at top show voltage waves. Oscillograms at bottom show current waves

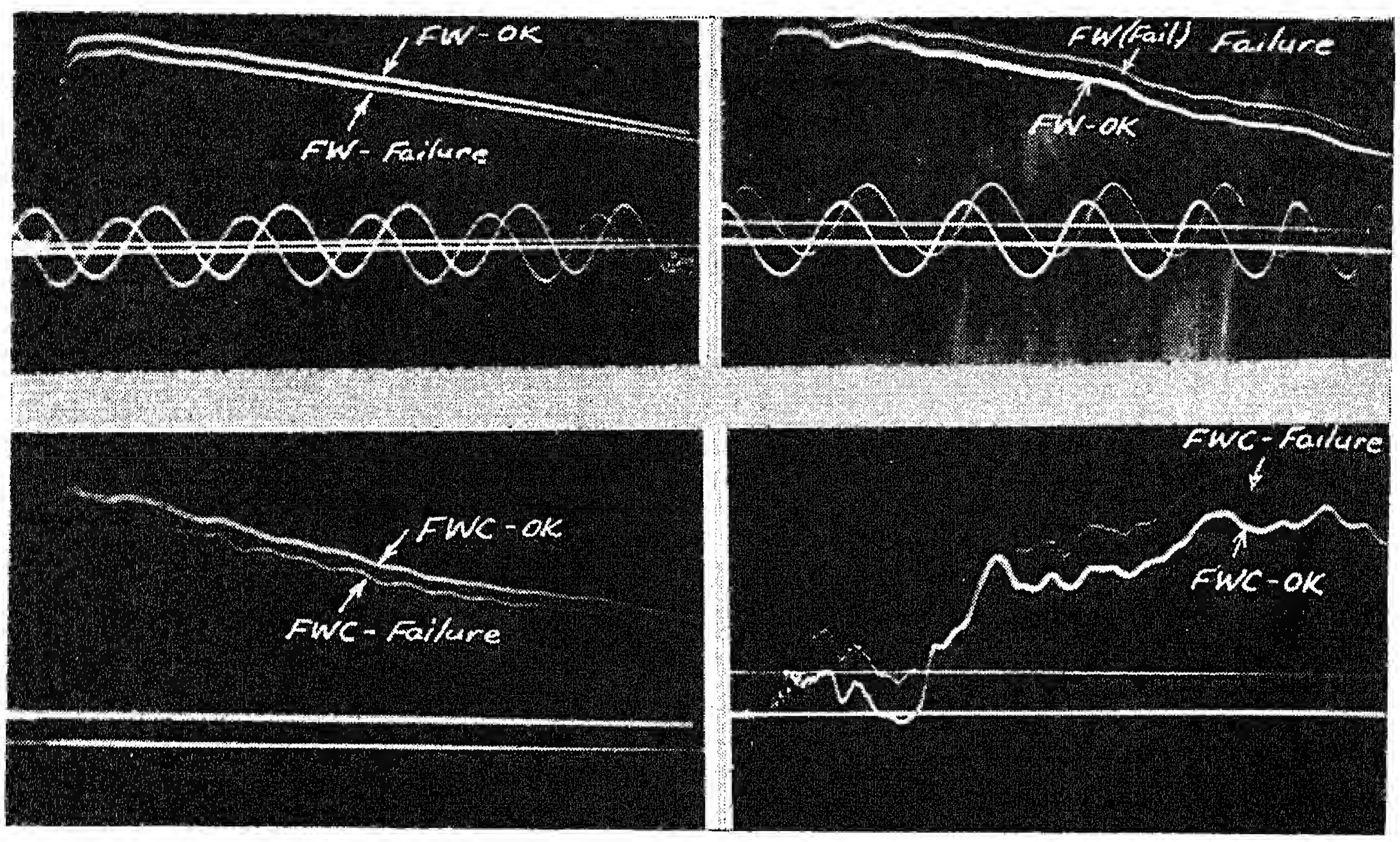


Figure 5. Current oscillograms indicate failure. Oscillograms at top show voltage waves. Oscillograms on bottom show current waves

failure in a unit is very simple. There is no question about it.

In many cases the voltage wave is sufficiently distorted to preclude any doubt as to difficulties in the transformer. These are called obvious cases and are illustrated in Figure 3. (In the oscillograms presented, unless otherwise stated, the ascribed symbols indicate: *FW*–full-wave voltage; *FWC*—neutral current measured during application of full-wave voltage. The prefix *R* as in *RFW* means the same quantities at reduced full-wave voltage, in practice about 50 per cent. The arrows are not intended to show wave-shape changes.) In many other cases only the most rigorous comparison and the trained eye of an experienced operator can detect differences in voltage wave shapes such as Figures 4 and 5. (In Figure 4 the left oscillograms show a condition where recovery was extremely slow. At the 50-per-cent level the failure persisted but cleared at the 20-per-cent level.) It is in these instances that the current waves are very helpful, because they magnify the fault indication several times and, therefore, leave little doubt as to the condition of the transformer, as also shown in Figures 3, 4, and 5.

Failure Severity

As stated previously, after failures are detected, the applied impulse-voltage crest is reduced in steps until failure does not occur. This level is called the critical-failure level. This critical level is plotted in Figure 6 in per cent of the ASA full wave as a function of the per-cent cases investigated. The upper curve applies to those cases where voltage and current waves showed a failure condition; the lower curve is for the cases where the failure was discovered principally by the current wave.

It is perfectly obvious that the cases picked out by the voltage and current waves have a high recuperative power, only 30 per cent of the cases having a critical breakdown less than 80 per cent as compared to 91 per cent of the cases discovered principally by the current waves. It is clear that the use of the current wave has resulted in discovering the types of faults which have the slowest recuperative power and are most likely to result in service failures.

Relative Value of Failure Indicators

Statistics on the effectiveness of the various failure indicators are shown in Table I.

Based on long experience with intensified observation of voltage oscillograms for possible failure indication, having had available the magnified failure indication of the current wave, the personnel conducting the commercial tests would discover practically all faulty units by the voltage method. Since this experience is not available in many impulse test groups, the types of voltage waves shown in Figures 4 and 5 are not considered as showing a fault. The statistics in Table I are not complete because in very obvious failure cases the oil was not observed and the transformer was not checked for noise.

The current wave is by far the most searching indicator closely seconded by the voltage wave. Noise appears to be a potent tool. In our tests the noise observation consisted of listening by ear through an insulating rod held against the tank. On one occasion a microphone pickup was used and gave a good indication of change in noise. This needs further investigation. Bubbles, smoke, or carbonized oil are most unreliable. Particularly in all cases where the current wave was the predominant indicator, bub-

Table I. Relative Value of Failure Indicators

Indicators	Per Cent of Cases Indicated
Current waves (cathode-ray oscillograph)	100
Voltage waves (cathode-ray oscillograph)	79
Bubbles, smoke, or carbonized oil	37
Noise	58
No bubbles	50
No noise	29
Neither bubbles nor noise	10
Not investigated for bubbles or noise	13

Table II. Winding Failure Detection

	Per Cent 1945–1948	Per Cent 1948–1951
All winding failures	100	100
Detected by current wave only	42.3	28.5
Detected by voltage and current wave	57.7	71.5

bles were not found. However, in one-third of these cases there was a distinguishable difference in noise.

Location of Failures

This is an important point, because frequently it is much more difficult to actually locate the point of failure than to detect the occurrence of a failure. Failures that occur outside of the winding structure proper are located easily and repaired easily. Failures within the windings are more difficult to locate and at times require complete unwinding and minute examination of the conductor. In such cases much time may be lost. Partly because of greatly increased developmental work and improved manufacturing conditions, and partly because of the use of the neutral current waves, certain flaws in design or manufacturing in windings have been avoided. This is shown in Table II, for two 3-year periods, which gives the types of failures and the percentage distribution. The failures most difficult to locate and most costly, and causing the greatest delay, were drastically reduced.

The same is true for winding failures as a whole which have decreased as compared to the total number of failures. In this connection it is probable that about 20 per cent of the transformers considered as failed because of detection principally by the neutral current-wave comparison would have been pronounced as having passed the test, and shipped, if extreme care in failure detection had not been used.

Table III. Classification of Failures (1933–1951)

Failure	Approximate Per Cent of Total
Winding to ground	5
Involving less than 5 per cent of winding	20
Involving more than 5 per cent of winding	25
Between leads, taps, and auxiliary components	40
Miscellaneous	10
Total	100

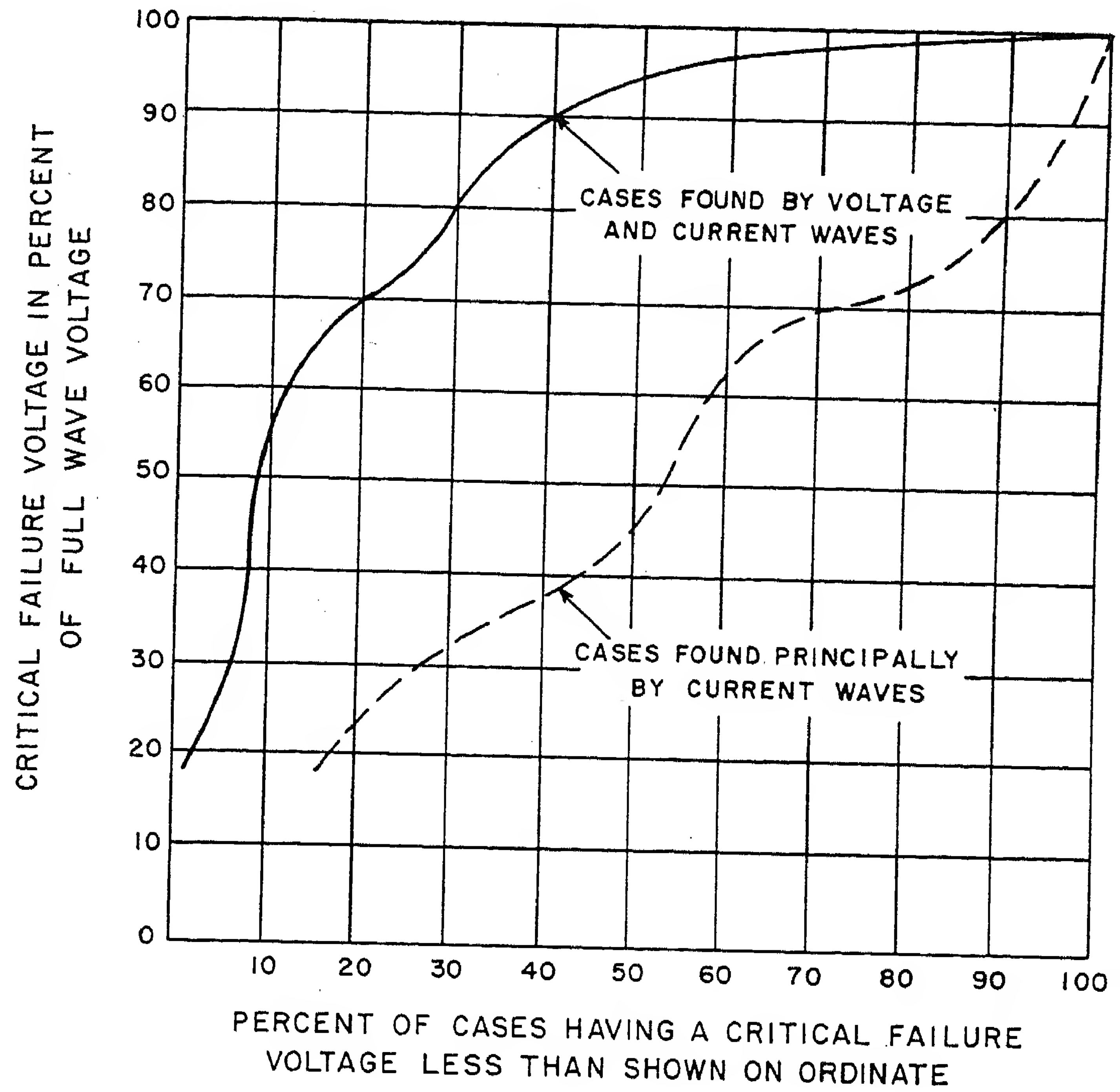


Figure 6. Critical breakdown levels after failure has occurred and has been discovered during full-wave application

From this strict adherence to the use of the most sensitive methods of failure detection, the proper lessons have been learned and applied to all transformers. As a consequence not only the winding failures but all failures have been reduced. At the present time the failure rate is approximately 6 per cent. It is doubtful that this percentage can be reduced greatly, unless design and manufacturing practices are absolutely frozen. This, of course, is undesirable because it would impede progress. Table III shows types of failures which have occurred since 1933.

Sensitivity of Detection of Failures with Current Waves

It has been proved by the authors' experience in commercial and developmental testing, as well as by many other investigators, that the current wave when used with the proper technique, will detect all failures, as previously defined, occurring on the full wave. It is not usable on steep front and chopped waves because slight changes in spark-over time of the chopping gap will result in current-wave changes similar to failure indications. Thus failures occurring during application of these waves, unless noticed by other means, will not be detected if the fault is not re-established during full-wave application. The same applies, of course, for the voltage oscillograms. To minimize such occurrence, the full wave must be applied immediately after the chopped wave. If applied within 1 minute, the chances that a fault may not recur are negligible. From Figure 6 it is evident that the faults most difficult to detect by means other than the current wave, have the slowest recuperative power and are certain to be detected by the current wave.

In some respects the current wave is too sensitive. It will record small disturbances produced by stray currents occurring for the following reasons:

1. Disturbances in the impulse generator gap, ground, and measuring circuit.
2. Disturbances in the core, which may be spitting between core sheets or between core and tank.
3. Entrapped air.
4. Some types of corona.

These disturbances appear as small pips or a flurry of low-amplitude high-frequency oscillations superimposed on the expected wave shape, without, however, causing any other change in that shape, see Figure 7. During the past 6 years, where the current wave has been used on all commercial and developmental testing, it has been found that it is practically impossible to locate the source of such disturbances which are inside the transformer. This is because such disturbances leave no mark. After thorough investigation of such cases, the following practice has been evolved to take care of such situations:

1. The impulse circuit is carefully examined and any suspicious connections or leads are rearranged.
2. The transformer is tipped to check for entrapped air.

This procedure eliminates many of the disturbances. In those cases where dis-

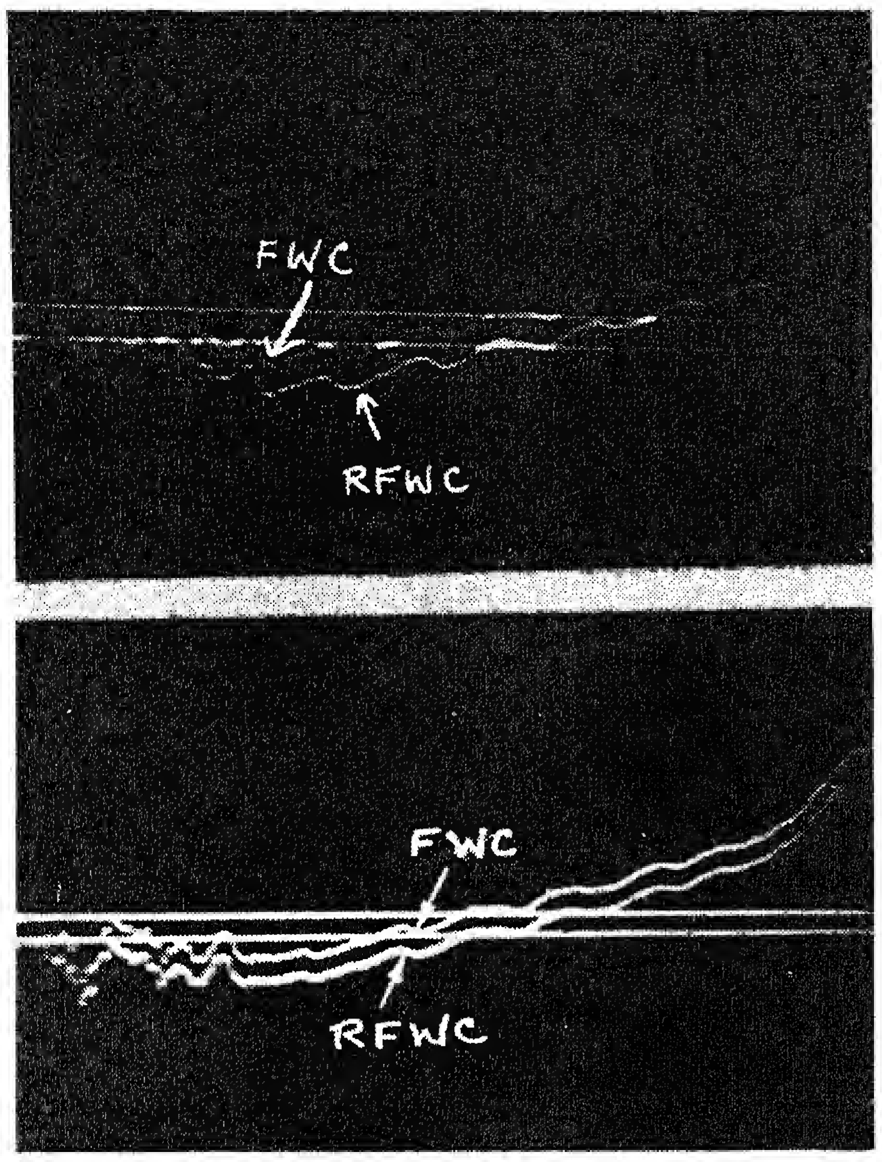


Figure 7. Current oscillograms showing slight disturbances superimposed on normal wave shape. Oscillograms on top show initial disturbances. Oscillograms on bottom show reduced disturbances after several full waves were applied

turbances persist, several full waves are applied and the resulting oscillograms are carefully compared. In many such cases the disturbance will disappear completely or diminish in intensity. In some it retains its original amplitude. So far no cases of small disturbances have shown an increase in intensity. Since the actual wave shape is not changed, no complete short circuit has been established in such cases. Since it seldom has been possible to determine the source of the disturbance and since the fact that the disturbance does not increase indicates that the cause is not progressive, the transformer is passed. Such disturbances never appear on the applied voltage waves and, therefore, information would be lost were the current wave not used.

This procedure permits the use of the current wave as the most sensitive failure indicator, without causing impractical delays in shipping transformers which may have some slight sparking under high impulse voltages in places where no danger to service life or performance is involved. At the same time, the occurrences are properly noted and serve as a continuous check on testing and manufacturing methods.

Low-Frequency Dielectric Tests

Low-frequency dielectric tests demonstrate, to a considerable degree, the following:

1. The impulse strength at the line end.
2. The strength for certain types of switching surges.
3. The ability to stand rated voltage continuously.

There has been a tendency to deprecate the importance of the low-frequency tests.

The present impulse test values were agreed upon about 15 years ago[8] after considerable discussion of impulse ratios, volt-time relationships, and so forth. The ratios between impulse and low-frequency dielectric tests have been retained even for transformers on grounded systems where the test levels were reduced to the next lower insulation class. There have been suggestions that these ratios are obsolete and should be revised.

Our tests show that on the average the present ratios are about right. Actually impulse ratios of insulation structures in oil vary over a fairly wide range. Since transformers contain a great variety of structures there obviously can be no one ratio that is right for all conditions. Until a great deal more evidence is presented to the contrary, the present test ratios should be maintained.

Co-ordination of insulation with respect to switching surges may become as important as impulse-voltage co-ordination. The relation between transformer-impulse and low-frequency tests and switching surge voltages is shown on Figure 8 for 230-kv transformers for various insulation classes. For the 180-kv or 75-per-cent insulation class the low-frequency test voltage crest is 2.6 times the permissible line-to-ground operating crest voltage. This is well within the expected range of switching surge voltages.

The information available on the strength of insulation in the region between the 1.5x40 wave and the low-frequency 1-minute test is limited to very small test specimens at relatively low voltages[9,10] and is not representative of over-all transformer insulation structures. The strengths of other types of structures used in transformers have not been investigated but limited data indicate large reductions in strength below the 1.5x40-microsecond wave strength. In the authors' opinion no change in low-frequency test requirements should be made until:

1. The wave shapes, amplitudes, and duration of switching surges are better understood.
2. The insulation strengths of actual transformer insulations are determined in the region discussed previously.

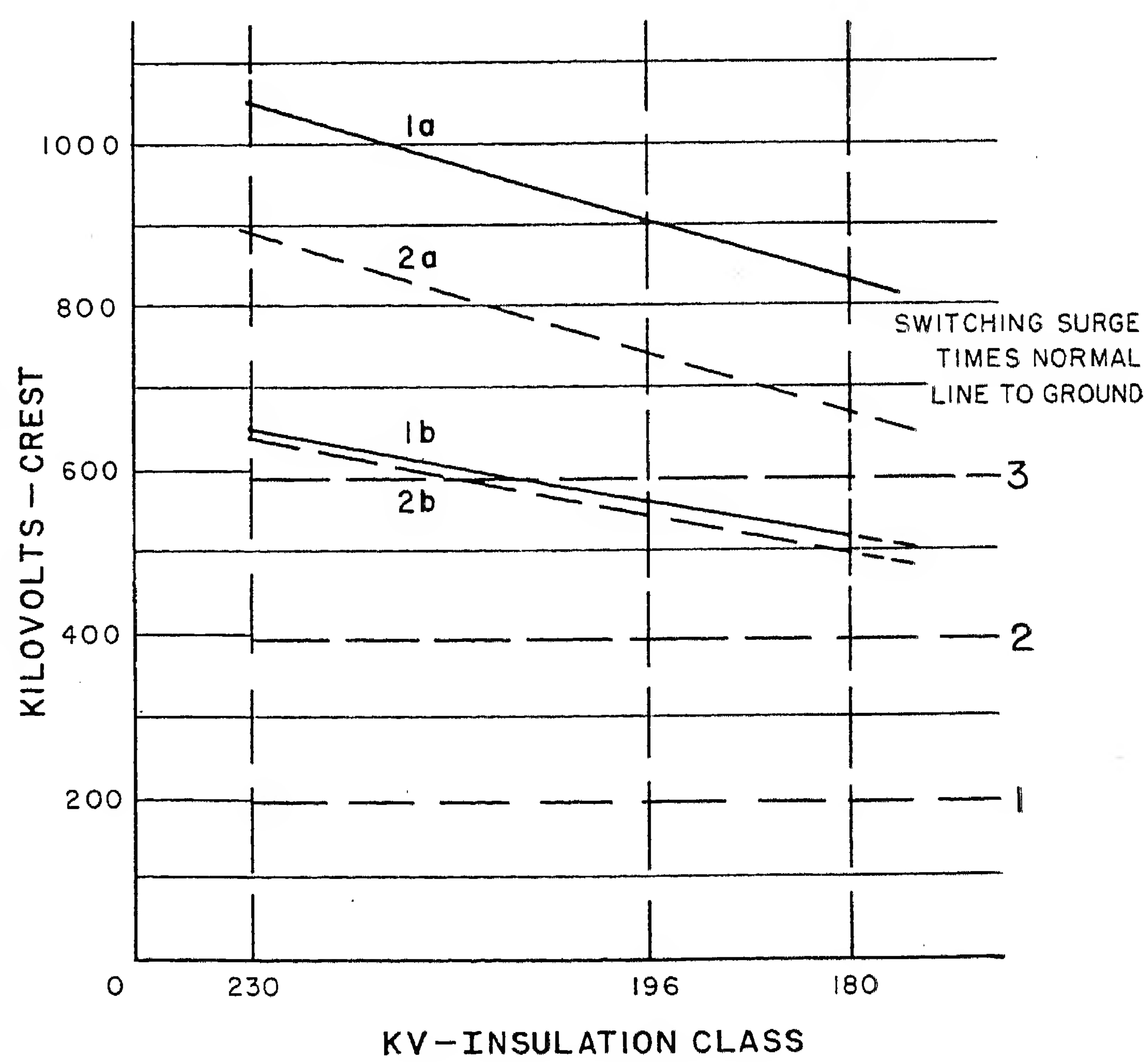
Conclusions

The following conclusions can be drawn:

1. The comparison between service and test failures justifies the policy of limiting impulse testing to the number of transformers that can be tested carefully, and with the best available failure detection techniques.
2. A simple full-wave test using quality control principles shows promise of proving manufacturing and design quality.
3. Impulse-test failures that do not produce bubbles, smoke, or noise are apt to be undetected unless neutral current measurements are made.
4. Impulse-test failures that are detected by neutral current measurements only may be those most likely to cause failures in service.

Figure 8. Relation between switching surge amplitudes, transformer test levels, and lightning arrester protective levels of 230-kv transformers for various insulation classes

1. Transformer tests
2. Lightning arrester protective level
a. Impulse
b. Low frequency



5. The sensitivity of the neutral-current method to stray sparking should not be used as an excuse for depending on less sensitive indicators of failure. Practical procedures are evolved easily.

6. Failures in service caused by transient overvoltages are rare even for transformers that have not been impulse-tested.

7. Low-frequency dielectric test values should retain the established ratio to the impulse-test values for the present.

8. Information on switching surge wave shapes, amplitudes, and durations should be obtained.

9. The strength of transformer insulations on various types of switching surges needs to be determined.

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Discussion

H. L. Rorden (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): In this day of reducing insulation levels of high-voltage transformers by more realistic balance between protective levels and basic insulation levels, the method of failure detection described by the authors is a definite step toward realism and elimination of unknown margins in our practices. Since the cost of insulation is very high in transformers, the "ignorance factor" has cost the industry heavily in past practices. By their method of detection of incipient failure the authors can more accurately determine adequate distribution of insulation to provide a well-balanced design relative to surge stresses.

The statement that failures in service are rare, even for transformers that have not been impulse tested, is significant in demonstrating that the blame too frequently is put in the wrong place. Also, the statement that a more realistic picture of the types of surges that occur in service is desirable, should be taken seriously by those who may be in a position to supply such information. With the surge testing equipment available in the large factories, manufacturers could go much farther still in their quest for adequate insulation distribution if they knew more conclusively what transformers are subjected to in service. There is not much use to provide insulation to withstand stresses that do not occur on transmission systems.

Lines of the Bonneville Power Administration are provided with ground wires only for station protection and extend about 1 mile on the 230-kv system. If the ground-wire design is adequate, direct strokes should not occur near transformers and so the steep wave front test is ordinarily not an indication of service requirement. Direct strokes occur to conductors a mile or more from stations and flashover at the nearest tower to the point where the hit occurred. The resultant wave at that point is, therefore, a steep front, chopped by insulator flashover to a level determined by the footing resistance and the line surge impedance. When this wave reaches the station, the steep front has been attenuated considerably so that the wave is somewhat like a long tail with an initial hump that is considerably below insulator flashover. While we are inclined to think this is best duplicated in the laboratory by the full-wave test, the authors' opinions would be appreciated.

F. J. Vogel (Illinois Institute of Technology, Chicago, Ill.): There are divergent opinions on some of the statements made in this paper. For example, some engineers did, and some still do, fear the possibility of damage in making impulse tests. My own experience is that except from a legal viewpoint, the use of impulse tests have led to negligible damage even from the first, and have raised the quality no end. Now some of the engineers formerly most reluctant to accept such tests are satisfied on the basis of the improvement in failure detection. It is felt that the authors are unduly concerned about the difficulty of finding damage due to the test.

So far as the service record is concerned, it is believed that it is exceedingly good for modern transformers. Statistical information on the untested units does not mean too much, because lightning in most stations is rare, and the chance of failure due to a defect then somewhat rarer. But I know of untested units failing in the field, and of defects in design, material, and assembly which showed up on some units on test and not on others. I believe if I had a big station, I would want the units to be tested.

The authors' comments regarding the relative value of failure indicators is affected by the design, in my opinion, and also by the techniques used in testing. The location of troubles is important, but by noting the time, the nature of the disturbance on the oscillogram, and other factors, it can usually be located quite closely.

It is interesting that the authors are advocating the use of current waves in testing and then find them too sensitive. Some people have not passed units with the slightest discrepancy in the current wave. Entrapped air should be avoided by vacuum filling. Corona under oil can be avoided. The core should be grounded. Therefore, entrapped air, corona, and "floating" core parts should not exist. Disturbances in the impulse generator circuit do occur but are on the front part of the wave. Some bushings and small parts have corona which show up as the authors describe and the use of current waves has required unnecessary refinement in their design. Irregularities in the surge generator circuit can not be prevented in many cases practically.

With respect to the ratios of 60-cycle and impulse-test values, that is a matter of design. Prevalent designs may change, and there may be progress in the art to make these ratios change. Even so, it is realized that changes in the standards will take place slowly and after such progress has taken place. This matter is of pressing importance now since it is a period where transmission voltages are being increased, and it affects cost and critical materials.

H. S. Hubbard (General Electric Company, Pittsfield, Mass.): As pointed out by the authors, the value of the impulse test first as a research and development tool, and second as a means of proof testing on a quality control basis, is well established. This is amply supported by the enviable record of transformers in service and the almost complete lack of failures due to lightning on systems protected with modern lightning arresters.

In the power-transformer field, however, impulse testing has not yet reached the stage where it is a simple "go" and "no-go" proposition. To apply the complete American Standards Association and National Electrical Manufacturers Association tests as now included in the standards to the very wide range of power-transformer ratings as manufactured today is both time consuming and uneconomical on a production basis. This is particularly true because of the high degree of technique required and the physical nature of the equipment involved.

Thus it is apparent that to take further advantage of impulse testing and to realize the economies which surely lie ahead, some modification in the test requirements for production testing of large numbers of units is definitely in order.

The authors have suggested that the full wave only might be considered as the basic transformer test. It is apparent from many available sources in the industry that this would be adequate for a vast majority of the requirements met with in service. If this be true, as we believe it to be, then there seems to be no merit in penalizing the industry as a whole for a minority of cases where ample means for the more complete tests are already available in the standards.

Accordingly a program has been instituted whereby a limited number of transformers of a given class are tested on a regular weekly schedule. This test comprises the application of a reduced 50-per-cent full wave followed by two full waves at the basic impulse insulation level. It is expected that this program will be continued and undoubtedly will expand in the future.

Such a simplification, as indicated, is practical, economical, and could be applied

on a production basis without any great sacrifice in the efficacy of the impulse test.

I. W. Gross (American Gas and Electric Service Corporation, New York, N. Y.): This paper is of particular interest to us as we have been impulse testing practically all 132-kv power transformers since this type of commercial testing of power transformers was first initiated, and to date none of those transformers which have been tested have failed in service from lightning. With such a record as this we must survey critically any proposed changes of impulse-testing procedure which it appears the authors are to some extent, recommending.

While the first transformers tested received chopped-wave and full-wave tests the steep front test was introduced later. This was done because it was felt that in service, transformers may be subject to any of the three types of waves, and therefore, a factory-proved test was desirable to show the transformers' ability to withstand such voltages. From the authors' statements it appears that they are now favoring full-wave tests only on the basis that by so doing more transformers can be tested commercially. In our early days of testing only one transformer of a bank of three or four was actually tested. However, the field record shows no failures of either the tested transformer or of the other units in the bank which, in many instances, did not receive any impulse test. We believe that it is important to maintain the test level at the regular specified value rather than endeavor to cut the magnitude of the voltage as has been attempted in some cases. Likewise, it seems desirable to retain at least one of the chopped-wave tests and the one chopped on the front of the wave is preferred to the one chopped on the tail.

It should be pointed out that the present tendency in commercial impulse testing indicates a preference to omit the 60-cycle excitation, a feature which reduces the severity of the test on the transformer, and is a compromise to simplify commercial testing procedure, although it does not conform to field conditions. Any attempt, therefore, to weaken or discount the present testing procedure should be viewed with considerable caution, and due consideration given to the adequacy of the test which we agree should, so far as reasonably possible, simulate field conditions.

The authors point out quite aptly that the switching surge strength of transformers is important, and that very little is known about this important feature at the present time because of the lack of data, and that the subject should be investigated further. We are fully in agreement with this reasoning and hope more attention will be given to the subject in the not too distant future, as it becomes increasingly important due to the present practice, and the impetus from some quarters, in reducing both impulse and 60-cycle test values on transformers.

J. H. Hagenguth and **J. R. Meador**: For the conditions described by Mr. Rorden for the lines of the Bonneville Power Administration, we agree that the full wave is the most realistic. A highly damped oscillation of moderate magnitude superimposed on the crest of the full wave would be even better, if obtainable.

We wish to thank Mr. Hubbard for his comments on impulse test simplification which are in complete agreement with the ideas expressed in the paper.

With regard to the comments by Mr. Gross, it is difficult to visualize the conditions under which the impulse waves reaching the transformer in service can be of the steep front test wave type where direct stroke shielding and lightning arresters are used.

As stated in the first paragraph of this closure, we believe the most realistic wave to be a full wave, possibly with an oscillation superimposed on the crest. Mr. Gross agrees that it is important to devote more attention to switching surges. It would be very helpful if Mr. Gross, Mr. Rorden, and others would publish any available data from their systems on the wave shapes, amplitudes, and duration of switching surges.

Mr. Vogel points out that there are divergent opinions on some of the statements made in this paper. On the question of damage during impulse test, our opinion can be restated in another way. We do not hesitate to make impulse tests because of fear of undetected damage. However, when impulse tests are made, we feel strongly that the best available means of failure detection should be used. While it cannot be proved that undetected damage during impulse test has caused any failures in service, a user would certainly be reluctant to install a transformer known to contain such damage. We do not find, as Mr. Vogel has, that disturbances in the impulse generator circuit always occur on the front of the wave. Furthermore, we believe that the pattern of such disturbances can be recognized and, after some investigation, disregarded. Small disturbances are occasionally found to be caused by entrapped air where, after vacuum filling, the oil must be lowered somewhat and then refilled to normal level during routine testing.

We believe that the obvious advantages of the neutral current method should not be lost by not using it just because of some slight imperfections for which practical procedures can be evaluated.

Sleet Melting on the American Gas and Electric System

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EARLY developments in sleet-melting and sleet-detection methods on the American Gas and Electric System were described in papers written in May and August 1939.[1,2] These methods were basically the detection of sleet formation by carrier-current means, and the isolating and connecting in series of sufficient lengths of 132-kv circuit to obtain the required sleet-melting current when the circuit is short-circuited at one end and connected directly to a 132-kv source at the other end. The present paper will outline the further developments in sleet-melting procedures which have taken place since these papers were written.

The American Gas and Electric System, an integrated network of 132-kv lines extending from Lake Michigan to the Virginia-North Carolina border, in 13 years has experienced a tripling of load with an increase in total 132-kv line mileage to 4,200 circuit miles, see Figure 1. While this has resulted in more lines to be melted, it is also obvious that the accompanying increase in generating and substation capacities, including extensive synchronous condenser and capacitor installations, has made available more and heavier sources of sleet-melting power and kilovars which are necessary for applying full-line voltage to a short-circuited line. As a matter of fact, even in the planning stages, where new lines are under consideration, the sleet- melting problem is set up and studied on the network analyzer along with the usual studies of power flow, voltage, and stability conditions. While large generating stations are still the principal direct sources or points of application of sleet-melting current, in many cases it is now possible, as a result of increase in concentration of power, to use substations for this purpose.

An example of a typical sleet-melting setup in the Indiana area of the American Gas and Electric System is given in

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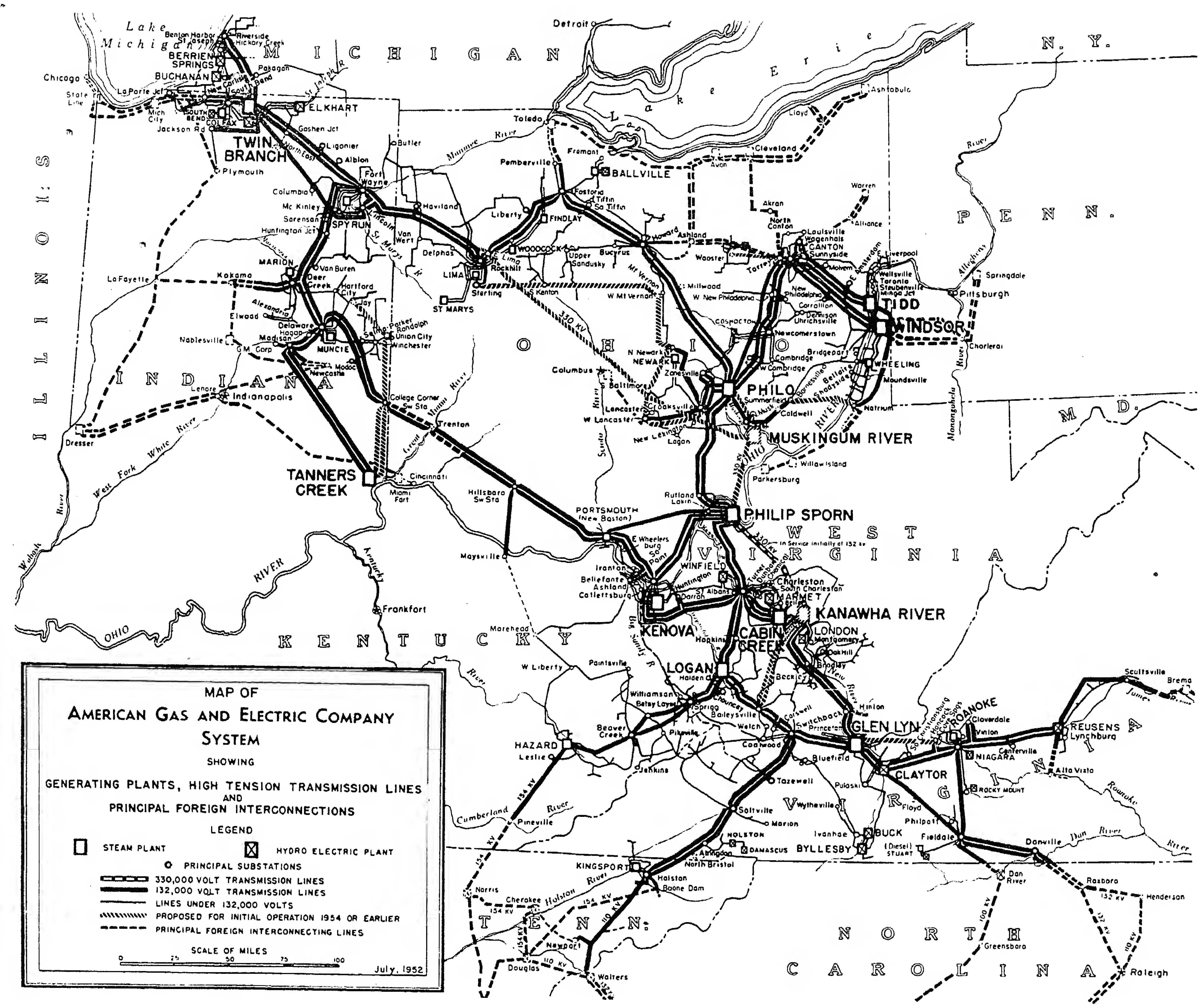

Figure 1

Figures 2 and 3. In this case the Fort Wayne Substation is used as a source for melting ice on 158.4 miles of 397,500-circular mil steel-reinforced-aluminum-cable conductor. The normal power flow conditions prior to sleet-melting are shown in Figure 2, while Figure 3 shows the power flow during the sleet-melting period. It will be noted that the voltage at Fort Wayne during sleet melting is depressed from a normal of 132,000 volts to 124,700 volts. This creates only a minor disturbance to the customers in the area, however, particularly since other voltage correction is applied on the subtransmission system at points at or near the load centers. In many cases substations are used as source for melting ice and result in only a slight depression of the 132-kv transmission voltage.

In general, with the system as now developed, there exists sufficient flexibility to obtain line combinations and short-circuit currents to do a reasonably fast melting job. There are frequent instances, however, where the necessary line combinations involve more than one conductor in series. In such cases it is necessary to obtain sufficient current for the larger conductor without exceeding the permissible current loading of the smaller conductor. Table I shows the actual sleet-melting current obtained in a number of such combinations, the short-circuit current in each case being adequate to melt ice on the larger conductor without endangering the smaller conductor, even if continued for extensive periods of time. The Turner-Logan-Baileysville-Switchback-Saltville circuit, as shown in Table I, consists mainly of 397,500-circular mil steel-reinforced-aluminum cable with approximately 3 miles of 292,000-circular mil steel-reinforced-aluminum cable. With an ice glaze of 1/2 inch, low wind velocity, and near freezing temperature, it is possible to melt ice on this circuit in approximately 15 minutes, using 750 amperes which is believed to be safe for the 292,000-circular mil steel-reinforced-aluminum cable conductor.

In a few cases it has been necessary to melt sleet on short sections of line at bus voltages under 132 kv. In these cases the short-circuit current has been applied from existing subtransmission buses. Examples of this are shown in tabular form in Table II.

In Table I the last three items include methods of melting certain line sections which previously had to be melted at subtransmission voltage and are shown under special voltage sleet melting procedures in Table II. In recent studies it was found that these lines could now be melted by using 132-kv sources.

In planning sleet-melting procedures, some of the factors that must be considered are:

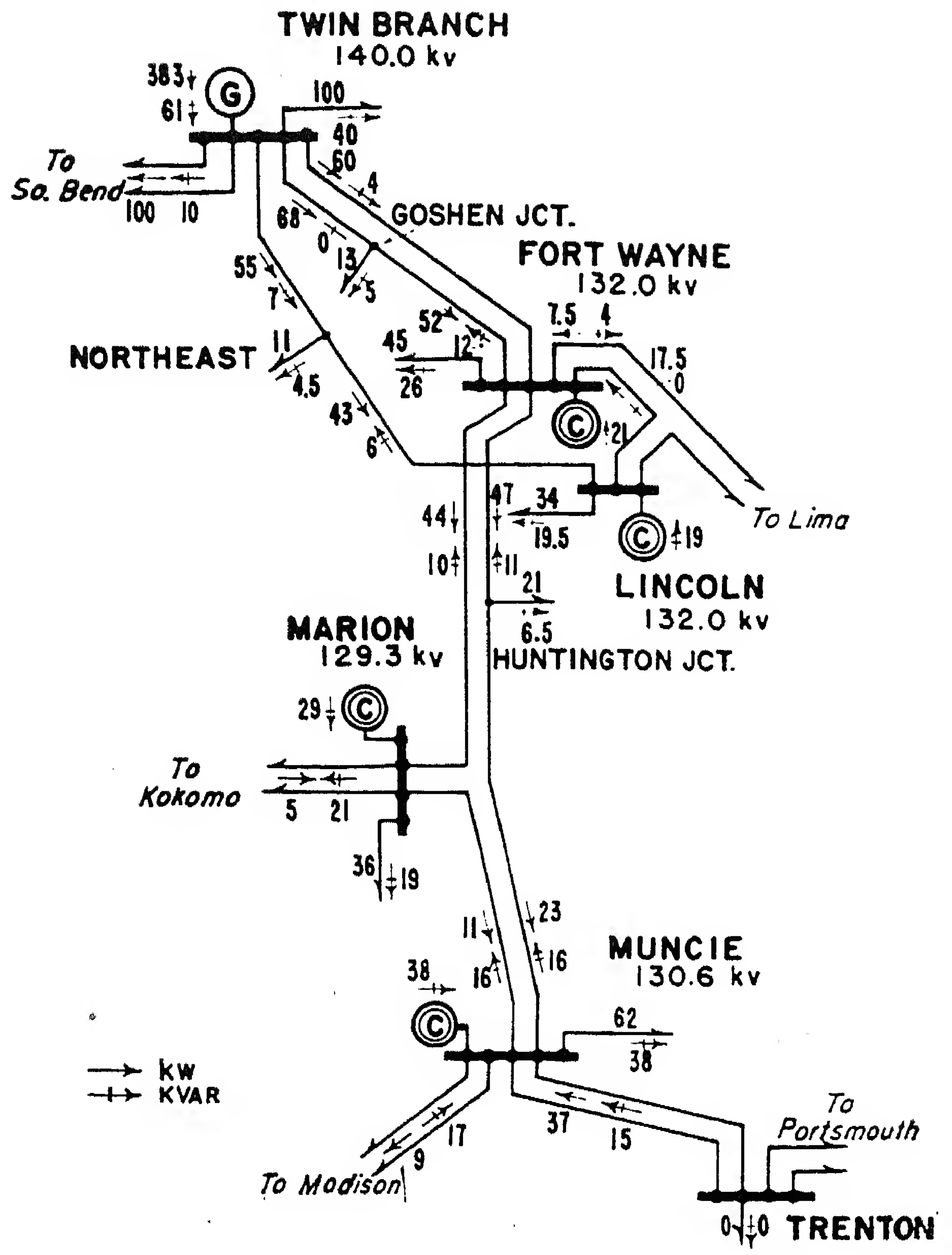

Figure 2. (left) Normal power flow

Figure 3 (right). Power flow during sleet melting

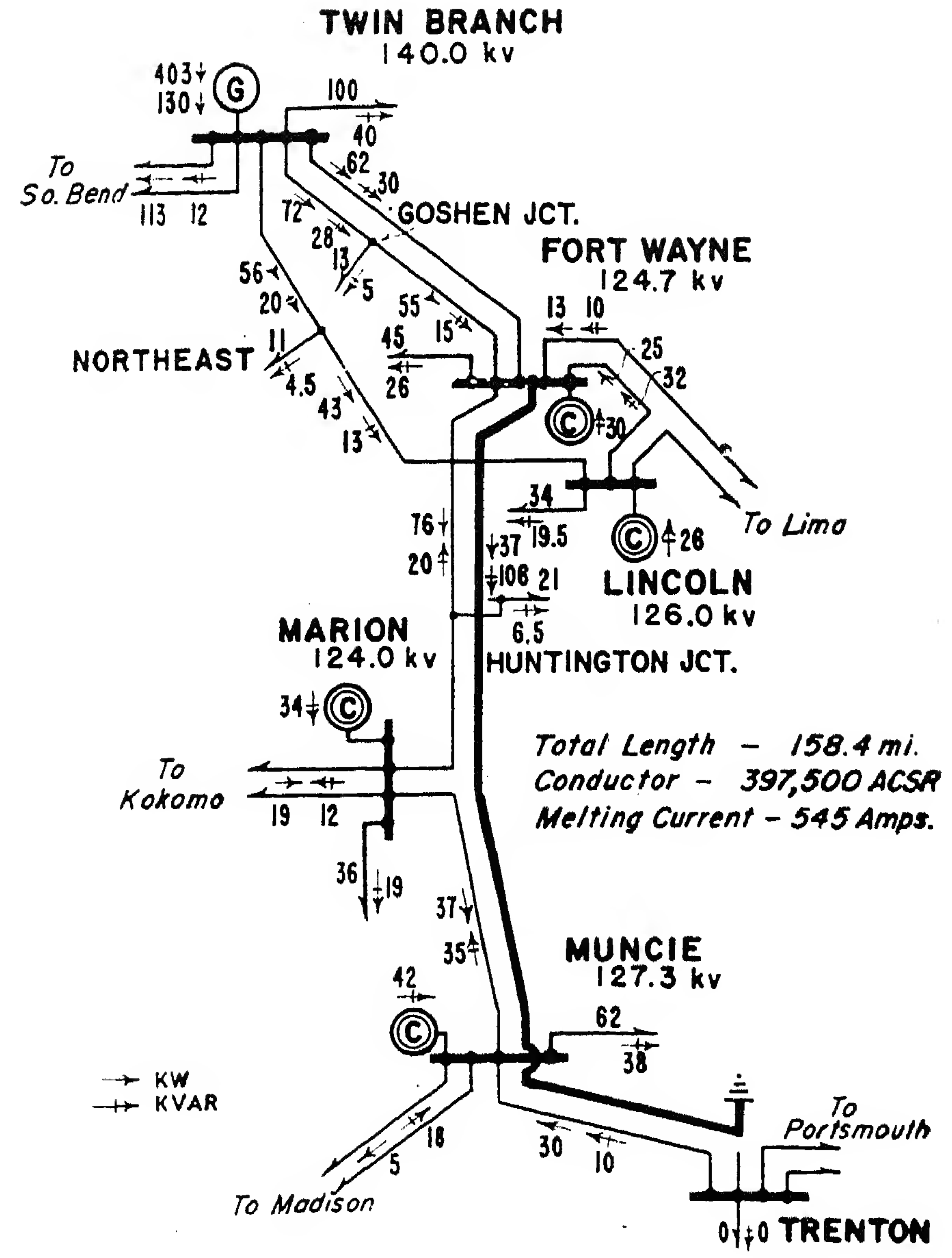

Table I. American Gas and Electric System

Typical 132-Kv Sleet Melting Procedure

Test Number	Lines	Length, Miles	Largest Conductor, Steel-Reinforced-Aluminum Cable Circular Mils	Smallest Conductor, Steel-Reinforced-Aluminum Cable Circular Mils	Melting Current, Amperes
1	Twin Branch-Ft. Wayne-Lima	129	397,500	397,500	740
2	Philo-Howard-Fostoria	126	556,500	397,500	765
3	Philo-Sunnyside-Windsor	129	336,400	200,000	670
4	Sporn-Portsmouth-South Point-Turner	143	477,000	397,500	680
5	Turner-Logan-Baileysville-Switchback-Saltville	127	397,500	292,000	740
6	Cabin Creek-Glen Lyn-Claytor-Fieldale	123	556,500	397,500	630
7	Madison-Tanners Creek-Special Double source from Ft. Wayne and Tanners Creek (Temporary)	82	636,000	636,000	760
8*	Ft. Wayne-Lincoln-Twin Branch-South Bend-New Carlisle-Michigan City	135	477,000	397,500	710
9*	Ft. Wayne-Lincoln-Twin Branch-South Bend-Plymouth-Michigan City	135	477,000	397,500	710
10*	Ft. Wayne-Lincoln-Twin Branch-Riverside	128	477,000	397,500	750

* Ice on certain line sections previously melted at 27 kv. procedures in 1952 permit melting at 132 kv.

Table II. American Gas and Electric System

Special Low-Voltage Sleet Melting Procedure Applied to 132-Kv Lines

Test Number	Lines	Melting Bus Nominal Voltage, Kilovolts	Length, Miles	Conductor, Steel-Reinforced-Aluminum Cable Circular Mils	Melting Current, Amperes
1	South Bend-New Carlisle-Michigan City	27	40	397,500	470
2	South Bend-Plymouth-Michigan City	27	40	397,500	500
3	South Bend-Twin Branch-Riverside	27	43	397,500	450
4	Tidd-Wagenhals	66	54	556,500	885
5	Tidd-Torrey	66	57	556,500	835
6	Tidd-Natrium-Summerfield	66	70	556,500	680

1. The selection of a line combination and grounding and switching arrangement which will give as high a current as possible without endangering equipment and service to customers.

2. The importance of melting as quickly as possible in order to keep the circuit outage time to a minimum. Early detection and the use of as high a current as possible will help to accomplish this, see Table III.

3. Any necessary adjustments in system load distribution and the need, if required, for help from interconnected companies.

4. The short-time current-carrying capacity of busses, by-pass facilities, current transformers, and so forth. In some instances it has been necessary to rebuild or replace equipment.

5. The adequacy of conductor joints to withstand sleet-melting currents.

6. The need for sending men to unattended stations and keeping them there throughout the switching period.

As pointed out in the previous papers, the behavior of carrier-current frequencies on transmission lines in the presence of sleet provides the key to the sleet-detection problem. It was observed on our own and on other systems, that during sleet storms carrier telephone channels became inoperative and that even telemetering and relay channels, which have a considerably greater margin, were frequently affected. Although few measurements have been made, it appears that signal attenuation during sleet formation might run from 10 to 20 times the values observed on a dry line. This increased attenuation, co-ordinated with observa-

Table III. American Gas and Electric System

Range of Sleet-Melting Currents for Various Conductors Near 30 Degrees Fahrenheit Ambient

Conductor	Short-Circuit Current Minimum Amperes	Maximum Amperes
200,000 copper	475	550
336,400 steel-reinforced-aluminum cable	550	700
397,500 steel-reinforced-aluminum cable	550	750
477,000 steel-reinforced-aluminum cable	575	800
556,500 steel-reinforced-aluminum cable	600	900
636,000 steel-reinforced-aluminum cable	625	1000

tions of ambient temperature and precipitation, was therefore used to give the operator an indication of the formation of ice. In order to detect this attenuation in the early stages of sleet formation, the equipment must be sensitive to small changes and yet relatively free from other disturbance. While special carrier-current transmitters and receivers could be designed for this purpose, it would be unduly expensive. Moreover, it was found that standard carrier-current relay equipment was almost ideally suited to the additional purpose of sleet detection for the following reasons:

1. The channel is confined to a single section.

2. The channel is protected by line traps at all terminals, thus reducing effect of abnormal switching.

3. The equipment is very simple and capable of retaining its characteristics

4. Operation takes place at high signal strength, minimizing the effect of noise and other signals.

Assuming that an instrument and test switch are available on the relay panel for testing carrier transmission, it is only necessary, therefore, to add a relay and resistor in the carrier set itself and a switch or additional switch contact at the relay panel. In fact, many sets today are already equipped with the relay and resistor as shown in Figure 4.

The theory of operation of sleet-detection equipment has been discussed in a previous paper[2] and amounts to readjusting the relay set so that it becomes sensitive to small changes of input signals. Its normal characteristic is substantially constant through a wide range of input signals, as shown in Figure 5. It will be noted that on an average transmission line the total attenuation between transmitter and receiver may be in the order of 20 decibels, as at point *A* on the graph. If we consider the receiver relay to be inoperative below 10 milliamperes, we would then have a margin of 25 decibels additional attenuation between point *A* and point *B*. Normally the equipment is adjusted to give all such possible margin. To have a carrier relay set act as a sleet detector, it is only necessary to introduce attenuation artificially through a resistor as shown in Figure 4 to have it operate at point *C* as shown in Figure 5. The receiver output will then change rapidly with any further increase in attenuation such as that due to ice formation. With this modified equipment, a simple test can be made by transmitting at one end of the line and observing receiver output at the other. The readings may then be crosschecked by reversing the signals.

When temperatures and precipitation are favorable for the formation of sleet, tests are made at frequent intervals and sleet melting is started as soon as readings drop more than 10 per cent below normal. Unfortunately this sleet-detecting system is unable to distinguish between a slight deposit of ice over an entire line and a dangerous concentration of ice within a few spans. Therefore, to be safe it is necessary to apply sleet-melting current as soon as possible before the second circuit on the same tower has excessive ice loading.

The tests on the sleet-detector equipment usually are made daily throughout

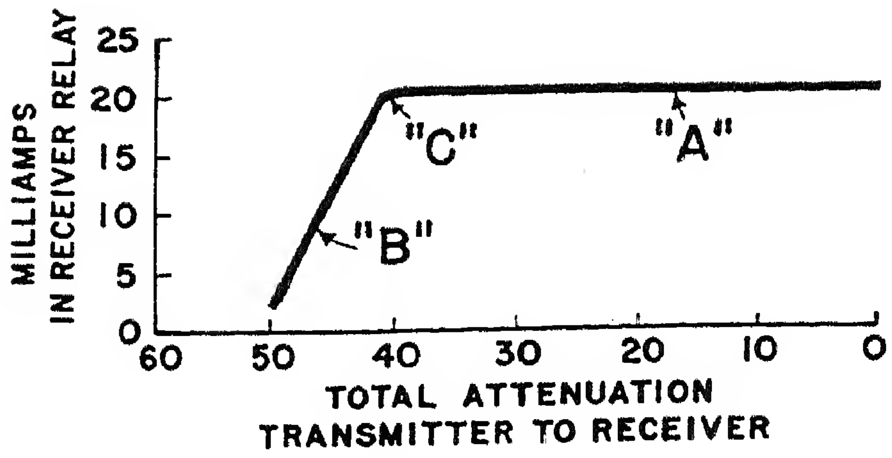

Figure 5. Sleet detector characteristic

the year since they provide an advance indication of impaired performance of the carrier sets, line tunings, line traps, and so forth.

In other papers[3,4] plans of the American Gas and Electric Company for the development of a 330-kv transmission system have been outlined. Several of these lines are now under construction, the first of which, operated initially at 132 kv, went into service in May 1952, between the Philip Sporn plant and the new Kanawha River plant. While there was some opinion that these lines would be substantially sleetproof, our considered conclusion was that this would be a dangerous assumption and that provision for sleet melting, at least as the new system became more fully developed, would be essential. Sleet melting, therefore, became a factor in the 330-kv line design and in the conductor selection.

The conductor finally selected was 1,269,000-circular mil steel-reinforced-aluminum cable, an expanded type with a diameter of 1.6 inches. Actual ice-melting tests on this conductor were carried out in co-operation with the Aluminum Company of America at Massena, using a test setup as shown in Figure 6. The 1/2 inch of ice shown in this picture was removed successfully with 1,200 amperes as illustrated in Figure 7. In Table IV the melting times required for this conductor at approximately 30 degrees Fahrenheit ambient are given for various current values. It will be noted that with 1/4-inch ice accumulation the time will vary from 45 minutes with 1,200 amperes to about 24 minutes with 1,600 amperes.

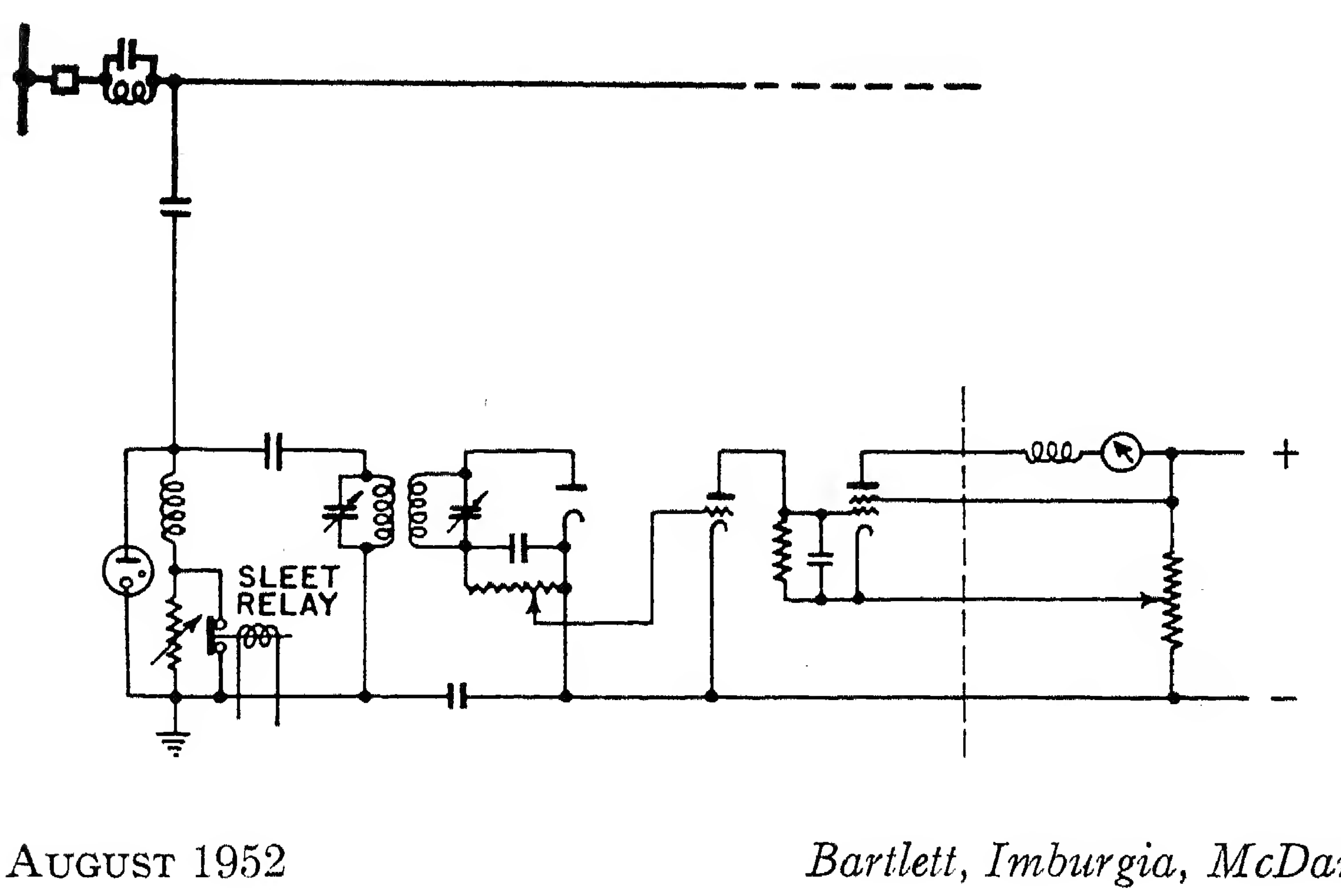

Figure 4. Typical schematic of pilot relay set—receiver

Table IV. American Gas and Electric System

Minutes Required to Melt Sleet on 1,269,000-Circular-Mil Steel-Reinforced-Aluminum Cable 30 Degrees Fahrenheit Ambient

Amperes	1/2-Inch Ice	3/8-Inch Ice	1/4-Inch Ice
1200	100	75	43
1300	85	65	36
1400	73	55	32
1500	64	48	28
1600	55	42	24

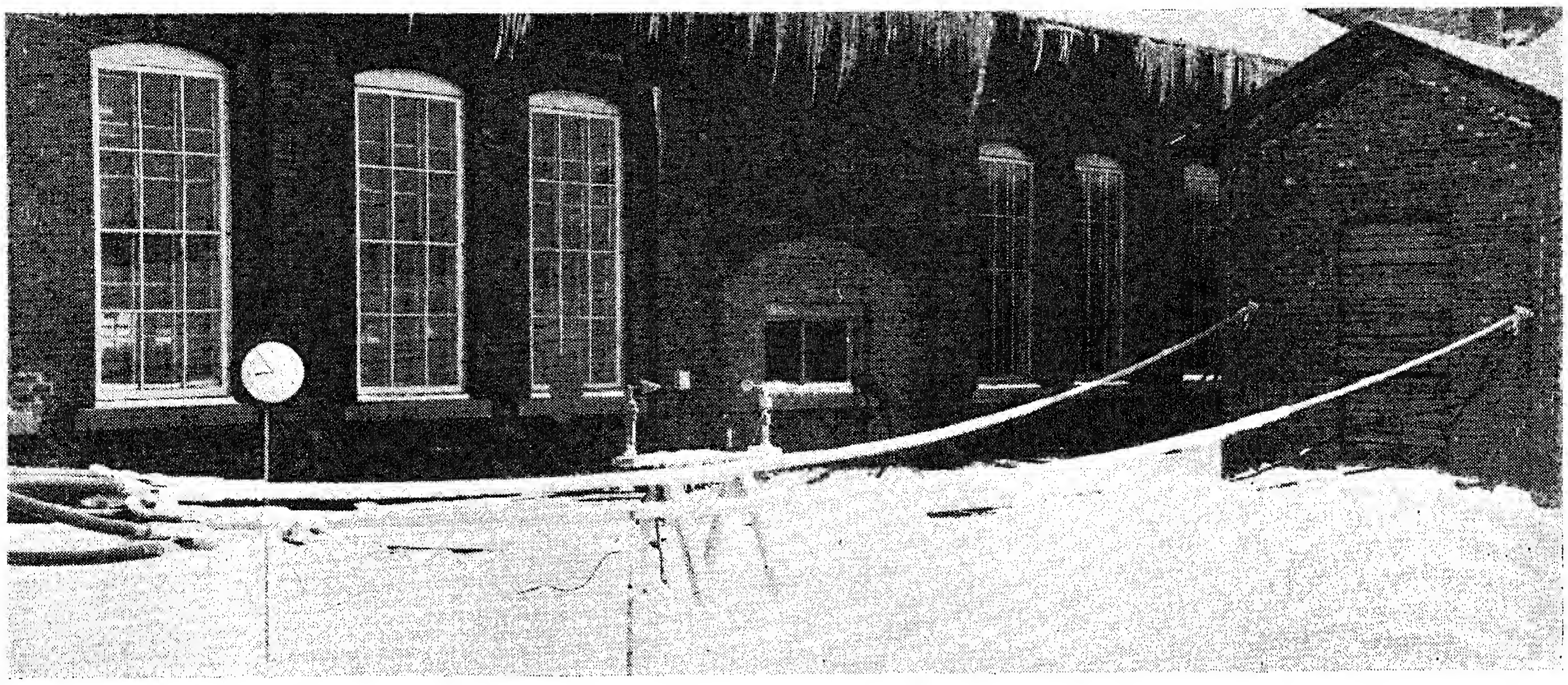

Figure 6. American Gas and Electric Service Corporation and Aluminum Company of America sleet-melting tests on 1,269,000 - circular mil steel-reinforced - aluminum-cable conductor — beginning of test

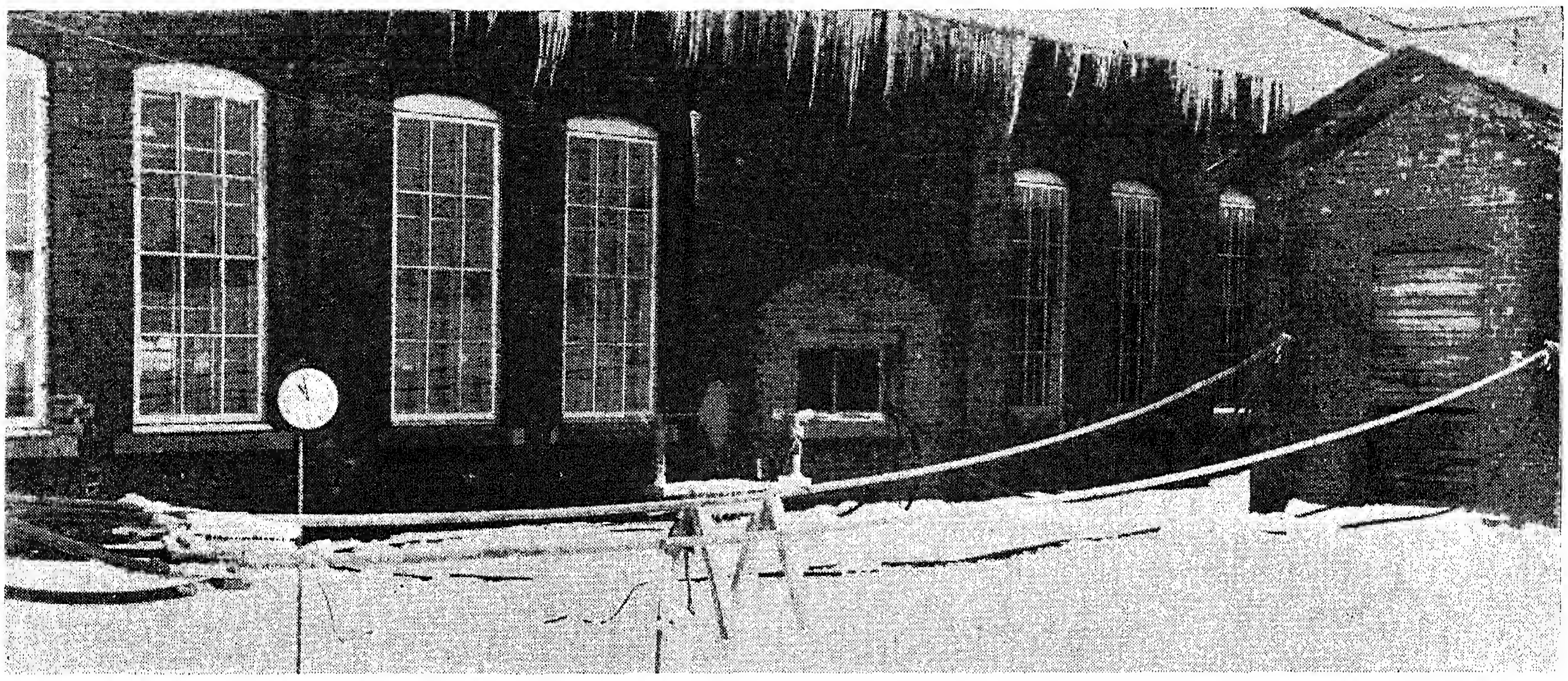

Figure 7. American Gas and Electric Service Corporation and Aluminum Company of America sleet-melting tests on 1,269,000-circular mil steel-reinforced-aluminum cable conductor —end of test

Melting sleet on these high-voltage lines at 330 kv obviously is out of the question in view of the power requirements of 700,000 to 900,000 kva. Network analyzer studies, on the other hand, show that it will be quite feasible to energize for sleet melting at 132 kv. At this voltage the required current of 1,200 to 1,600 amperes can be obtained with line lengths varying from 85 miles to 65 miles respectively. For 1,200 amperes and a line length of 85 miles, the power requirements will be approximately 275,000 kilovars and 30,000 kilowatts. This compares with about 375,000 kilovars and 40,000 kilowatts for a current of 1,600 amperes and a line length of 65 miles.

Experience through the years gives us a feeling of satisfaction regarding the methods employed in melting sleet at full-line voltage. It must be conceded that upon occasion operators will go through the process of melting only to find subsequently that the sleet indication resulted from rain or other signal impairment. However, the cost of going through this process is small and adequately justified by the assurance that steps are taken on the safe side.

References

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2. Carrier Attenuation Discloses Glaze Formation, **G. G. Langdon, V. M. Marquis.** *Electrical World* (New York, N. Y.), August 12, 1939.

3. System Economics of Extra-High-Voltage Transmission, **H. P. St. Clair, E. L. Peterson.** *AIEE Transactions*, volume 70, part I, 1951, pages 841–51.

4. The 300/315 Kv Extra-High-Voltage Transmission System of the American Gas and Electric Company, **Philip Sporn, E. L. Peterson, I. W. Gross, H. P. St. Clair.** *AIEE Transactions*, volume 70, part I, 1951, pages 64–74.

Discussion

Conrad F. De Sieno (American Gas and Electric Service Corporation, New York, N.Y.): In determining sleet-melting procedures for a system as extensive as that of the American Gas and Electric Company, time-saving approximations are often necessary and justified. Although the a-c network analyzer is ideal for studying sleet-melting source-line-ground combinations, its usage for this purpose often can be limited to a few basic studies to determine mainly which stations have the capability of supplying the necessary reactive power without excessive voltage drop and, therefore, which can be used as sleet-melting source points. The sleet-melting setups then can be determined by trial-and-error calculations using the nominal voltage at the source and the series line impedance to the grounding point. The resulting current should lie within the range given in Table III of the paper for all conductors involved; also, all the factors enumerated in the paper should be considered for the setup in question. The actual source voltage and line-charging current can be estimated and the sleet-melting current can be corrected accordingly, but this is seldom necessary because great accuracy is not required.

This procedure can be simplified further by using curves of sleet-melting current, power, and reactive versus line length, drawn for specific conductors and based on nominal source voltage. Figure 1 of this discussion shows a curve used as an aid in planning sleet-melting setups for our 330-kv lines. These lines are being equipped with 1.6-inch-diameter 1,275,000-circular-mil expanded steel-reinforced-aluminum-cable conductors at an effective spacing of 29 feet.

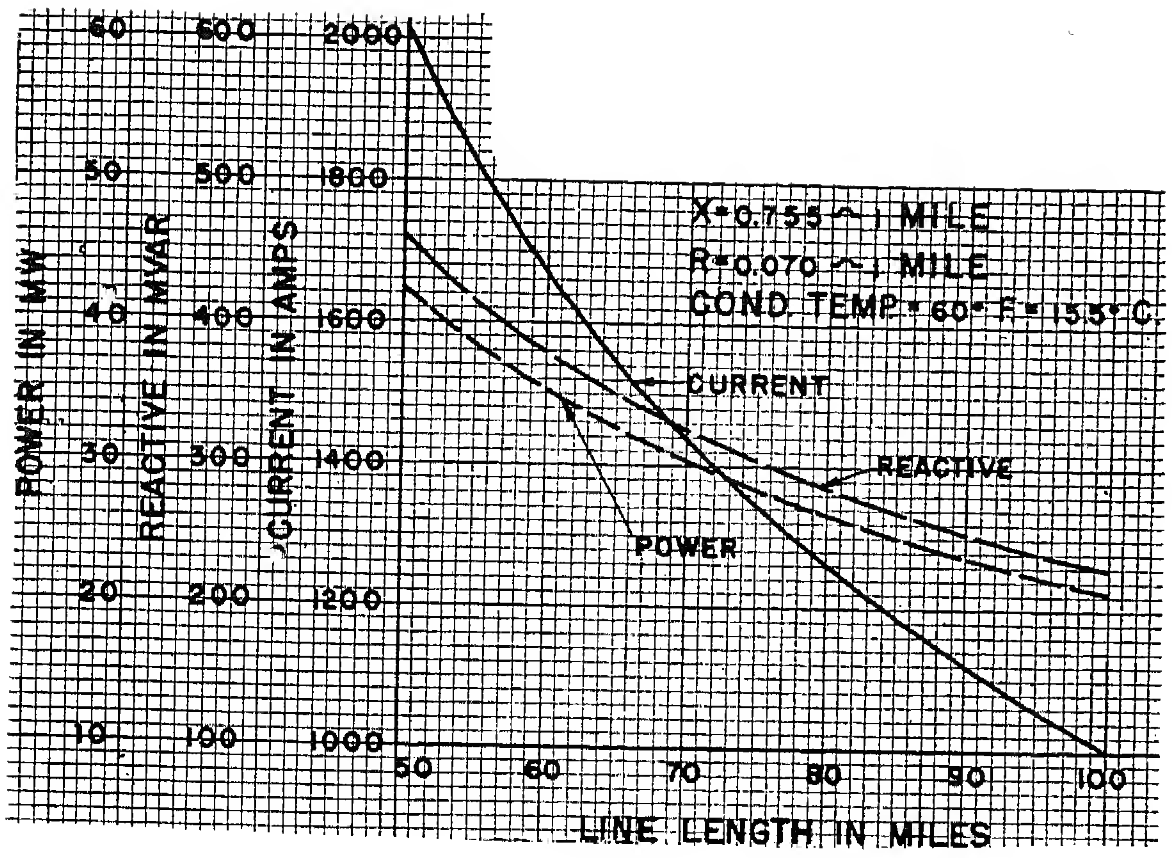

Figure 1. Sleet-melting current, power, and reactive versus line length for 1.6-inch-diameter 1,275,000-circular-mil expanded steel-reinforced aluminum-cable conductor based on 132.0-kv applied voltage

The conductor temperature of 60 degrees Fahrenheit is based on temperatures measured during recent sleet-melting tests by the Aluminum Company of America. Although this conductor is capable of carrying 2,000 amperes for a limited time during sleet conditions, our sleet-melting current is limited, at present, to 1,600 amperes by the existing 132-kv terminal equipment which must be used for this purpose. Currents below 1,200 amperes, on the other hand, require excessive time for sleet melting. The curve readily shows that for this range of currents our sleet-melting setups are limited to 330-kv lines between 63 and 85 miles in length with 132-kv sources which must be capable of supplying between 200,000 and 400,000 kilovars.

Power Limits of Transmission Lines

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THIS paper presents the results of studies of the factors which influence the power limits or design loadings of transmission lines. Recent investigations[1–8] have shown the principal factors determining the loading of transmission circuits, particularly those for distances of 100 to 600 miles. With the recent progress in high-voltage transmission, the economic factors of the previous studies may need review. The present investigation extends the earlier studies to include lines from 10 to 600 miles in length and correlates the basic philosophy of transmission-line loading for both long and short lines. This study indicates a further confirmation and refinement of the previous studies and at the same time gives results in the range of shorter length lines.

Since there is a continuing trend toward the use of higher voltages, it is desirable to study their possibilities more completely on a generalized basis. The economic feasibility of using high voltages for even shorter distances than were previously thought practicable results from a continuing reduction in line and terminal equipment insulation and from a fuller exploration of the economic advantages realized by using larger transformer units, higher-interrupting-capacity circuit breakers, and larger conductors. Concurrently, the increasing cost of right-of-way in many areas and the high cost of transmission-line construction through rugged terrain has increased the economic attractiveness of higher voltages with the correspondingly heavier loadings.[5,9]

From the present study, conclusions are drawn which indicate the influence of various factors on transmission-line loading over the entire range of distances expected for 230 kv and higher. The results are general as regards the stability limits and may be applied to lines of any voltage level. The economic comparisons in this study pertain to the 287-kv voltage level, which is about in the middle of the range for 230 kv to 380 kv and, hence, is fairly representative of the general economic picture for all voltages 230 kv and above.

Where a higher-voltage line is to be superimposed on an already existing high-voltage system, the anticipated growth of load on the proposed transmission line (load-time curve) may be an important factor in determining when an investment should be made for the various steps which are taken before a complete high-voltage backbone and the ultimate line loading are realized. The present study considers only the ultimate line loading and, hence, the factor of time, that is, transmission line load growth, has been neglected. This study shows, therefore, the end economic result for the various factors considered; namely, voltage, distance, intermediate switching stations, series capacitor compensation, and generator and receiving-system reactances. After a consideration of the effect of these factors on the ultimate line loading, the anticipated load growth of the proposed line can be used, for example, in the determination of when the introduction of a higher voltage becomes desirable.

Conclusions

The results of the study of transmission lines 10 to 600 miles in length are summarized in this section:

1. The representative economic power limits of two parallel high-voltage transmission lines are shown on Figure 1 in terms of the surge impedance loading (SIL, see Table I). These stability limits, which are based on clearing a 3-phase fault by switching out one line section, depend on the assumptions for receiving-end reactance. For example, for the range of receiving-end reactance considered here, the stability limit of two lines 50 miles in length varies from approximately 1.75 to 2.5 per unit SIL which for a 330-kv system is a receiving power of 950 to 1,350 megawatts. These stability limits are for a system in which the rating of the sending-end generator and transformer is equal to the stability limit of the system.

2. The basic system configuration, that is, the number of intermediate stations and the per-cent series compensation, which gives the representative economic power limits shown in Figure 1 is essentially independent of the range of receiving-end reactance considered here. For example, for a line length of 100 miles and for receiving-end reactances in the range from 0.14 to 0.25 per unit, the economic limits are realized with one intermediate station and zero series compensation.

3. The economic advantages resulting from heavier circuit loadings, reduced insulation, lower line losses, larger transformer units, and higher-interrupting-capacity circuit breakers can be realized most

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The author wishes to acknowledge the many helpful suggestions of Mr. S. B. Crary and the assistance of the General Electric Company network analyzer staff in making the stability studies.

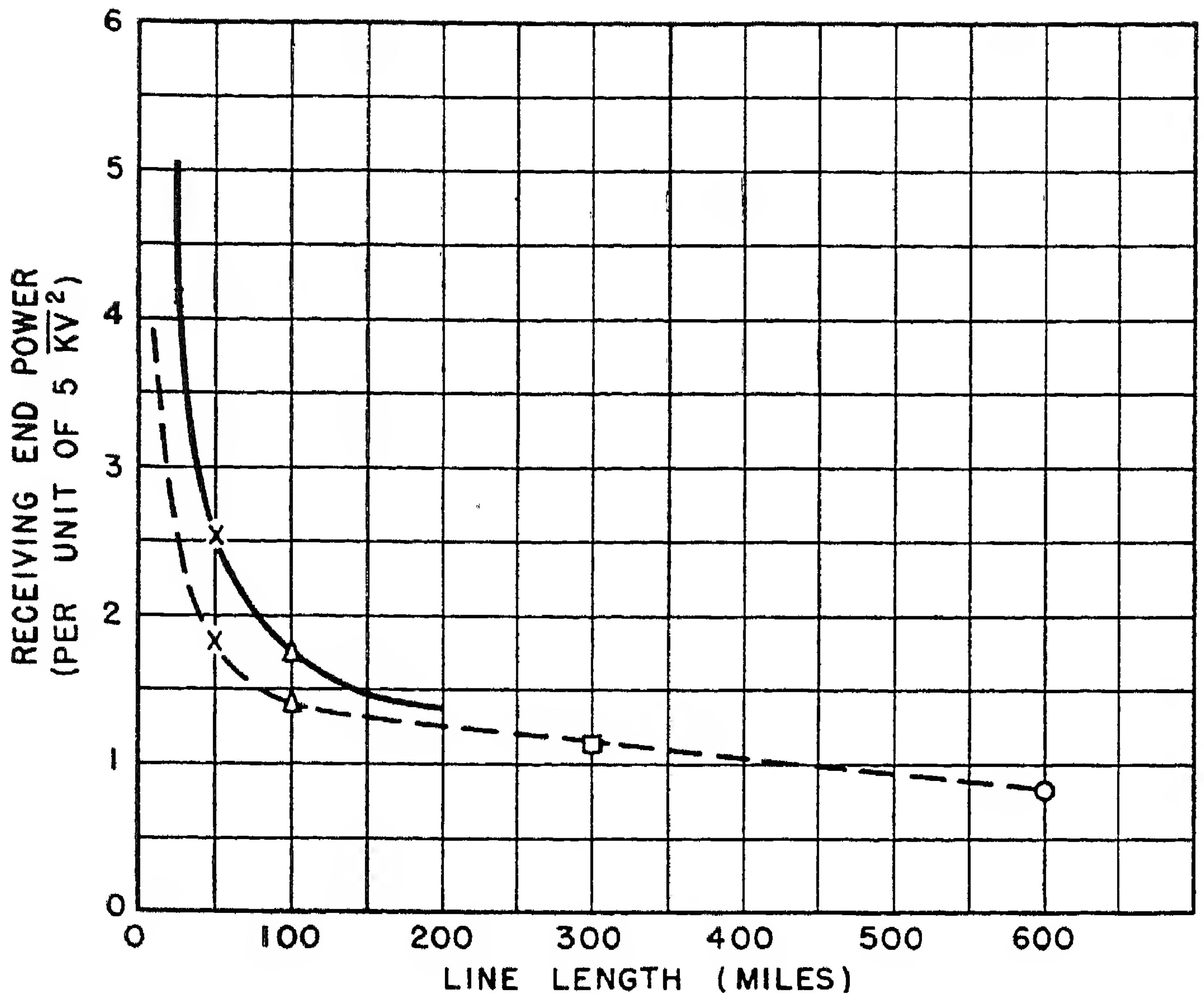


Figure 1 (left). Representative economic power limits for two parallel high-voltage transmission circuits, based on assumptions of Figure 2 and for 287-kv system cost data

- - - - $x_m = 0.25$ Receiving-end reactance

———— $x_m = \frac{0.25}{P_s}$ Receiving-end reactance

Basic System Configuration

Distance, Miles	Point Symbols	Number of Equally Spaced Intermediate Switching Stations	Per-cent Series Compensation
50 or less	X	0	0
100	Δ	1	0
300	□	4	50
600	○	5	75

readily in many cases by using higher-voltage lines.

4. Intermediate switching stations are an economic means of increasing the power limits of lines as short as 60 miles. The economic number of switching stations is a function of the line length, the number of parallel circuits, and the per-cent series compensation. For any distance, as the number of parallel lines increases and/or the per-cent series compensation increases, the economic number of switching stations decreases (spacing of stations increases). For example, for a 300-mile line with two parallel circuits, the economic spacing of switching stations increases from 50 to 75 miles as the series compensation increases from zero to 75 per cent; whereas for a 600-mile line with 75-per-cent compensation, the economic spacing of switching stations is 100 or 150 miles corresponding to two or six parallel lines.

5. Series capacitor compensation installed at the intermediate busses may be practical and effective for two parallel circuits approximately 250 miles or longer. Series capacitors when used in this manner should be provided with quick reinsertion protective equipment.

6. For long lines, the economic design loading per circuit may be increased by paralleling more than two lines. For a 600-mile line with six parallel circuits, three intermediate switching stations and 75-per-cent series compensation appears to be an economic arrangement for increasing the line loading. The feasibility of such an arrangement depends in part on local geographical conditions.

7. Since a large part of the cost of electric energy transmission is in the initial investment, the cost of energy varies almost inversely with the load factor and directly with the annual charge on investment, but the relative costs of energy received for various system arrangements would not be altered for reasonable variations in load factor or annual charges.

Table I. Surge Impedance Loadings (SIL) per Circuit for Representative Voltage Levels

Line, Kilovolts	Single Conductor SIL, Kilowatts*	Dual Conductors SIL, Kilowatts**
115	33,000	
138	47,600	
161	65,000	
196	96,000	
230	132,000	165,000
287	206,000	257,000
330	272,000	340,000
380	361,000	451,000
450	505,000	632,000

*Surge impedance kilowatt loading per circuit when the surge impedance is 400 ohms, corresponding approximately to high-voltage circuits using single conductors per phase without series or shunt compensation.

**Surge impedance kilowatt loading per circuit when the surge impedance is 320 ohms, corresponding approximately to high-voltage circuits using dual conductors per phase without series or shunt compensation.

System Studied

The system used in these studies is shown in Figure 2. The voltage at the sending and receiving high-voltage busses is unity under normal conditions, and the power factor of the receiving system is unity. In all cases the line conductor was 800,000-circular mil copper equivalent ($r/x=0.1$). Transient stability was tested with a 3-phase fault on the sending end. This fault was cleared in 3 cycles (0.05 second for circuit breaker and relay operation) by switching out a line section.

The stability limit obtained from a transmission line of a certain distance depends on the sending- and receiving-end reactances. This is particularly true with short distances where the line impedance may be a relatively small percentage of the total impedance. The sending-end generator and transformer rating, which determines the sending-end reactance, was always chosen to correspond to the transient stability limit of the system, whereas two conditions of receiving-end reactance were investigated. For one condition the receiving-end system had a fixed capacity and a relatively high reactance, and for the other condition the capacity of the receiving-end system was increased (the reactance decreased) as the stability limit of the system increased.

The performance of the various schemes is reported in terms of the surge impedance loading,[8] see Table I; hence, the results are general as regards the stability limits and may be applied to lines of any voltage level. The economic considerations however, must take into account the actual costs at the different voltage levels. Cost data for the economic studies is given for a 287-kv system on Figure 2. The cost of transformers is not included in the economic comparisons since the study concerns only one voltage level and the cost per kilovolt-ampere of transformers is assumed to be essentially constant. The economic studies are based on the power transferred at the stability limit.

The series capacitors where used are installed at the intermediate stations and are provided with quick reinsertion protective equipment since series capacitors used in this manner are more effective for increasing the power limit for a given capacitor reactive kilovolt-ampere than are capacitors which are used in the middle of a line section and which are not provided with the aforementioned protective equipment. Per-cent series compensation is taken here to mean the per-cent compensation of the reactance of the line; that is, for a 600-mile double-circuit line with 0.8-ohm reactance per mile, 75-per-cent compensation distributed in five intermediate stations corresponds to

$$x_c = \frac{600\times0.8\times0.75}{2\times5} = 36 \text{ ohms per phase per station or 0.18 per unit on a kilovolt-ampere base of } 5(\text{kv})^2.$$

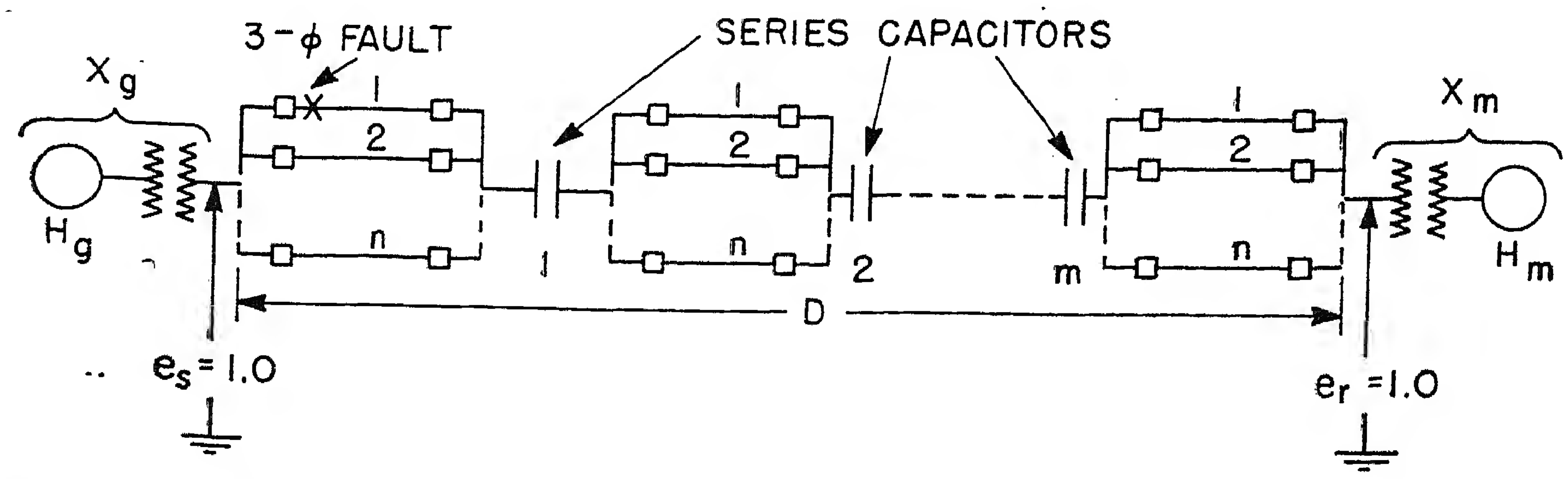


Figure 2. System studied, where D = length of line in miles, n = number of circuits in parallel, and m = number of equally spaced switching stations

Line: 800,000-circular mil copper equivalent ($r/x = 0.1$)
$r = 0.08$ ohm per mile
$x = 0.8$ ohm per mile
$y = 5.2$ micromhos per mile
$f = 60$ cycles per second
Power factor at receiving end $= 1.0$
Fault clearing time $= 0.05$ second
Series capacitor reinsertion time $= 0.05$ second
Base kilovolt-amperes $= 2.5(n)\overline{kv}^2$

Sending end:
$x_g = \frac{0.3}{P_s}$, $H_g = 3.5P_s$ } Generator kilovolt-amperes assumed equal to kilowatt output at stability limit (P_s)

Receiving end:
Part A—$x_m = 0.25$
$H_m = \infty$
Part B—$X_m = \frac{0.25}{P_s}$
$H_m = \infty$

Cost Data (287 kv):
Single circuit steel tower line = \$50,000 per mile
Double circuit steel tower line = \$75,000 per mile
Circuit breakers = \$200,000 per circuit breaker position
Receiving-end compensation = \$10 per reactive kilovolt-ampere
Series capacitor = \$10 per reactive kilovolt-ampere
Losses = \$150 per kilowatt + \$0.004 per kilowatt-hour (50-per-cent load factor)
Annual charge = 12.5 per cent

Intermediate Switching Stations Without Series Compensation

Previous studies[1,2,4,5] have shown the desirability of using intermediate switching stations every 100 to 150 miles to increase the stability limit of transmission lines 200 to 600 miles in length and for two, three, or four parallel circuits. This study has extended the previous work to include lines from 10 to 600 miles in length for the assumptions shown in Figure 2.

Figure 3 shows how intermediate switching stations increase the transient stability limit (based on assumptions of Part A, Figure 2, which correspond to a fixed receiving system) for two parallel circuits without series compensation and for transmission-line lengths of 10, 25, 50, 100, 300, and 600 miles. The effect of distance on power received at the transient stability limit for two parallel circuits without compensation is given for zero, two, five, and infinite number of switching stations on Figure 4(A). An infinite number of switching stations corresponds to clearing a fault without increasing the system impedance. For zero switching stations, the power received at the stability limit is about 16 times greater at 10 miles than at 300 miles and the maximum line length for stable power transfer under transient conditions is about 300 miles. With two stations, power can be transferred about 400 miles; and with five stations the maximum distance for stable power transfer without series compensation is approximately 550 miles. For an infinite number of switching stations, the power received is about eight times as great at 10 miles as it is at 600 miles. The stability limits presented here are for a system where all of the generation is transmitted over the entire distance. If some of the generation were used to supply local loads or if there were connections to intermediate loads or systems,[1] the stability limits would be increased since for such conditions the sending-end generator and transformer would have a larger rating than without the local or intermediate load and would have a smaller reactance.

Figure 4(B) shows the effect of dis-

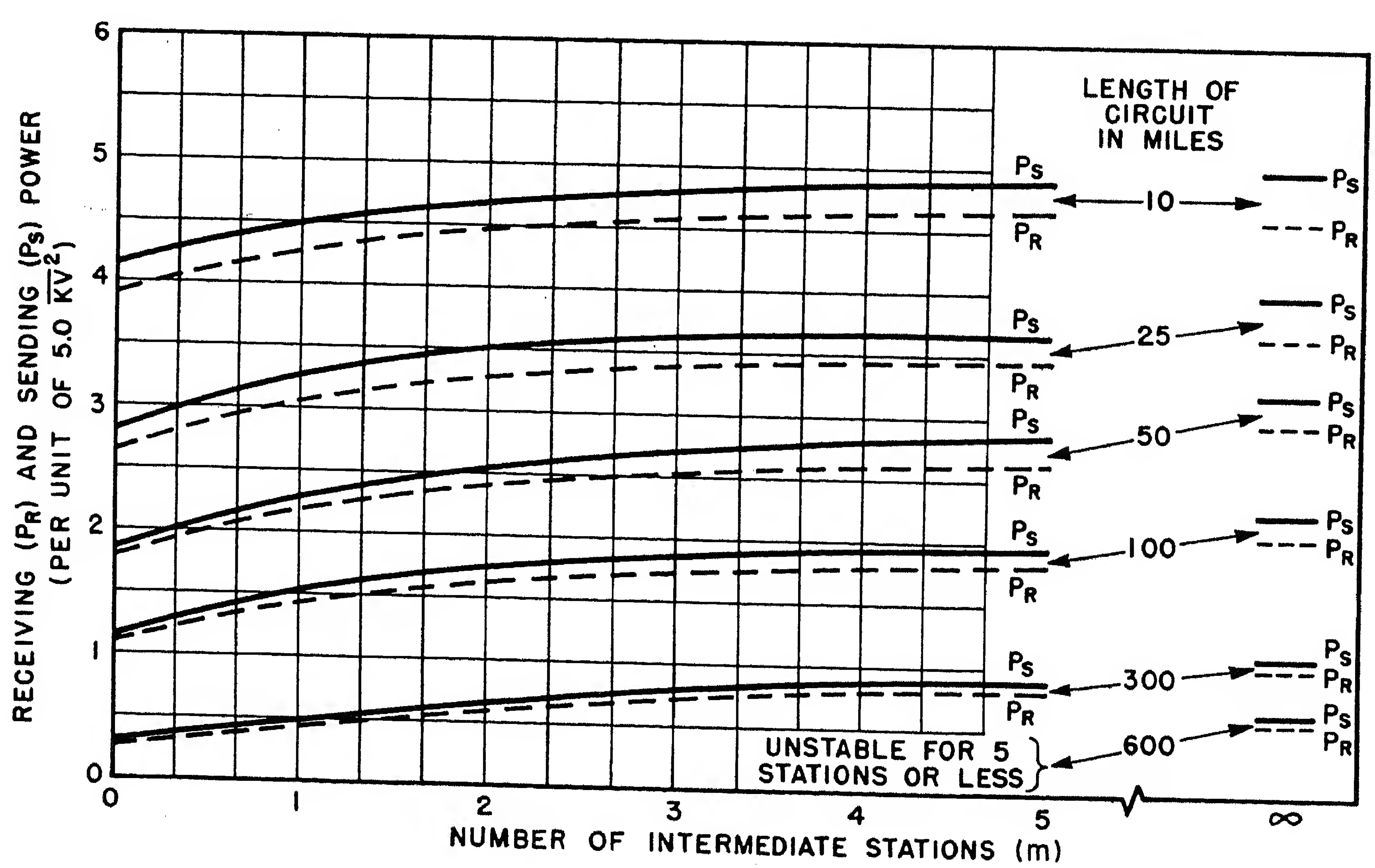


Figure 3. Effect of equally spaced intermediate switching stations on transient stability limit for two parallel circuits with zero series compensation. Based on assumptions of Figure 2, Part A

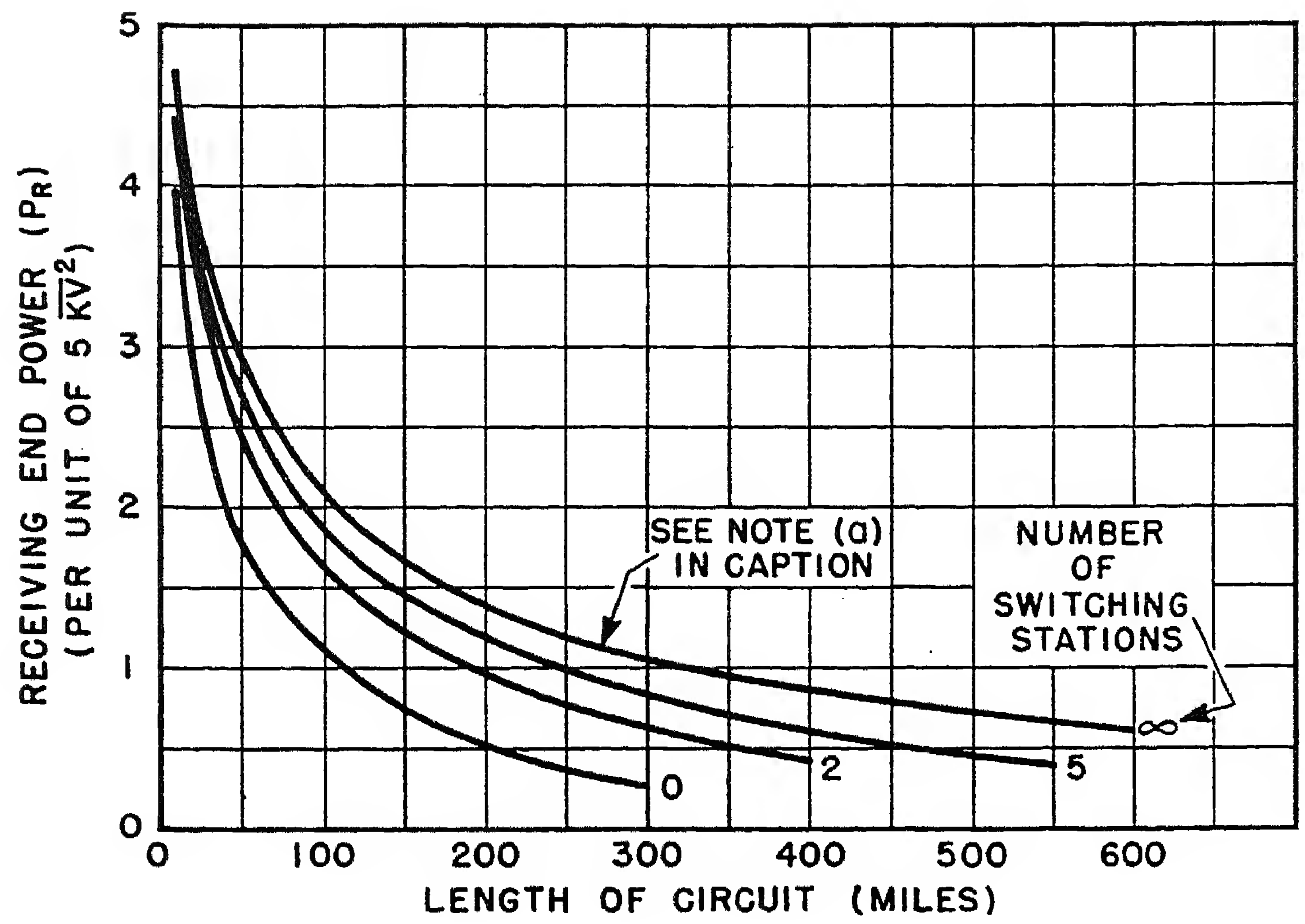


Figure 4(A). Effect of distance on power received at transient stability limit for two parallel circuits with zero, two, five, and infinite number of switching stations and with zero series compensation. Based on assumptions of Figure 2, Part A

NOTES:

a. The curve for ∞ number of switching stations also shows the stability limit per circuit for an ∞ number of parallel lines with any number of intermediate switching stations if the unit for the ordinates is 2.5 $\overline{kv}^2$

b. Six-hundred-mile line unstable for five or less stations, 450-mile line unstable for two or less stations, and 350-mile line unstable for zero stations

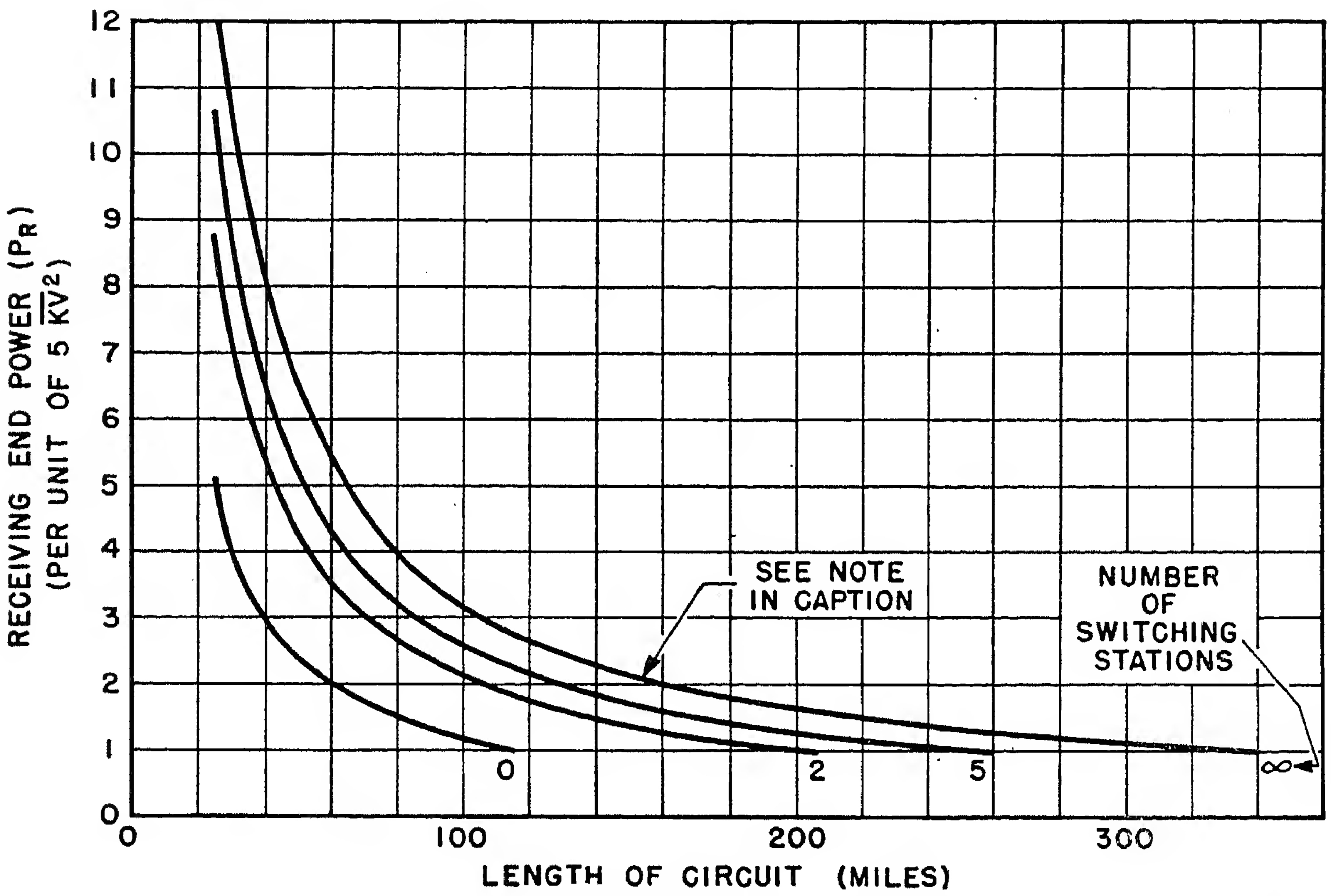


Figure 4(B). Effect of distance on power received at transient stability limit for two parallel circuits with zero, two, five, and infinite number of switching stations and with zero series compensation. Based on assumptions of Figure 2, Part B

NOTE: See Note a, Figure 4(A)

tance on power received at the stability limit for the assumptions of Part B, Figure 2, which correspond to a receiving system that increases in size as the stability limit of the system increases. The assumptions for Part A and Part B, Figure 2, are equivalent when the stability limit (P_s) is unity. Figure 4(B) shows only the stability limits which are greater than unity.

Figure 5 shows a comparison of power received at the stability limit for the two assumed receiving-end reactances ($x_m = 0.25$ and $x_m = 0.25/P_s$) and for zero and infinite intermediate switching stations. At the shorter distances where $P_s > 1.0$, the stability limit is considerably greater for $x_m = 0.25/P_s$ than for $x_m = 0.25$. For example at 25 miles and zero switching stations the stability limits have a ratio of 2-to-1 for the assumed receiving-end reactances. This may be attributed to the relatively large percentage of the total system impedance that is concentrated in the receiving end under the assumption that $x_m = 0.25$.

The effect of number of parallel circuits on the transient stability limit is demonstrated for a 25-mile line on Figure 6 which shows that for a fixed receiving-end reactance the transient stability limit per circuit is increased from 2.8 to 3.75 as the number of parallel circuits is increased from two to six. The maximum possible stability limit per circuit for an infinite number of circuits in parallel is 4.0 at 25 miles. The curve labeled "infinite number of switching stations" on Figures 4(A) and 4(B) can also be interpreted, as indicated in the figure caption, to mean the power received per circuit for an infinite number of parallel lines, regardless of the number of the intermediate switching stations. Hence, the maximum benefit, on a per circuit basis, to be gained from a stability standpoint by paralleling any number of circuits up to 600 miles is given on Figures 4(A) and 4(B).

Figure 7(A) shows the effect of intermediate stations on the cost of transmitting electric energy for two parallel circuits operated at 287 kv and for a fixed receiving-end reactance. The economic comparisons employ the cost assumptions on Figure 2 and exclude the cost of step-up and step-down transformers. For a 300-mile circuit the economic number of intermediate switching stations appears to be five. For the 100-mile circuit, the economic number of switching stations is one, and for the shorter lines, 50, 25, or 10 miles in length, intermediate switching stations can not be justified from an economic standpoint. Figure 7(B) shows similar results for the same system as Figure 7(A) but for a receiving-end reactance (Figure 2, Part B) that decreases as the stability limit increases. It is interesting to note that the economic number of switching stations for a given distance is essentially independent of the receiving-end reactances considered in this paper.

Figure 8 shows the cost of electric energy received as a function of the number of circuits in parallel for a 25-mile line. These results show that as the number of parallel circuits increases, the cost of electric energy also increases slightly. This at first appears to be irrational; however, it must be noted that the energy costs were calculated on the basis of the power received at the stability limit and hence the conclusion to draw from Figure 8 is that the line has been loaded beyond the economic limit (loading limited by real and reactive losses) and that for more than two 25-mile lines in parallel, stability is not a limiting factor.

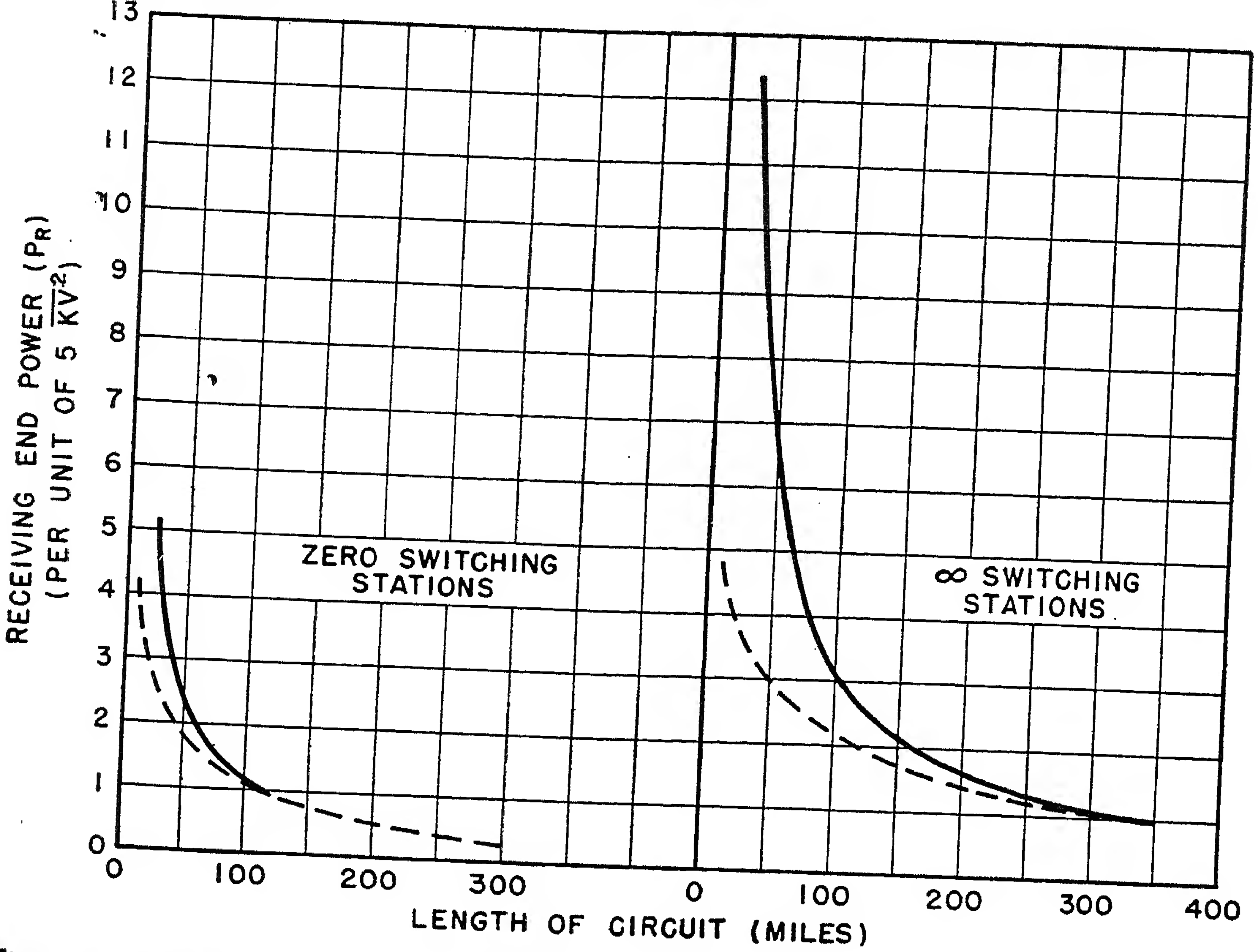


Figure 5. Effect of receiving-end reactance on power received at the stability limit for two parallel circuits without series compensation. Based on assumptions of Figure 2

- - - - $x_m = 0.25$ } Receiving end-reactance

——— $x_m = \dfrac{0.25}{P_s}$ } Receiving end-reactance

Intermediate Switching Stations with Series Compensation

The use of series capacitors to increase the stability limit of transmission lines has received considerable study.[6–8,10,11] Several installations[7,12–14] are in operation and others are being contemplated. With the development of protective means which are capable of reinserting the capacitors quickly after system faults it is practical to place the capacitors between the bus sections of the intermediate stations where they can be used in the most economic manner. Consideration is given here to the effect of series compensation on the stability limit of lines 300 to 600 miles in length and the resulting increase in power transfer at the stability limit is evaluated economically.

Figures 9(A) and 9(B) show the effect of intermediate switching stations on the transient stability limits for two parallel circuits with 50- and 75-per-cent series compensation respectively and based on the assumptions of Part A, Figure 2, which correspond to fixed receiving-end reactance. Figure 9(A) shows that with 50-per-cent compensation, two parallel 600-mile circuits must have at least four intermediate switching stations to transmit power stably. Figure 9(B) shows that this same system compensated for 75 per cent of its series reactance must have at least three stations for the stable transmission of power. Figure 9(C) shows the effect of intermediate switching stations on the transient stability limit for six parallel circuits 600 miles in length. With zero compensation the six parallel circuits are unstable for less than two switching stations. With three switching stations, the 600-mile 6-parallel circuit system which is compensated for 75 per cent of its series reactance transmits about three times as much power as the uncompensated system. It is interesting to note that the stability limit per circuit is about 1.75 times as great for the 6-circuit case as for the 2-circuit case.

These same results are illustrated in a different manner on Figure 10 which shows the effect of series compensation on power received at the stability limit for typical transmission systems. It is shown here that for the 2-parallel circuit 300-mile line with five stations the stability limit is doubled by increasing the series compensation from zero to 75 per cent. For two parallel, 600 mile circuits with five switching stations and 75 per cent compensation, the power received at the stability limit is approximately 0.82 surge impedance loading, whereas with zero compensation the circuit is unstable. For the 600-mile 6-parallel circuit line with three switching stations, the power is increased about 2.5 times by using 75-per-cent compensation.

The economic aspects (excluding costs of step-up and step-down transformers) of using various numbers of switching stations and different degrees of series compensation are illustrated in Figures 11 and 12. Figure 11(A) shows the effect of intermediate stations on the transmission costs (excluding transformers) of electric energy received for two parallel circuits 300 miles long on a 287-kv system. The most economic system arrangement is shown to be four intermediate switching stations with 50-per-cent series compensation although an arrangement with three intermediate stations and 75-per-cent compensation is nearly as effective. Figure 11(B) shows the transmission costs (excluding transformers) of electric energy received for two parallel, 600-mile circuits on a 287-kv system. Here it is seen that the system with five switching stations and 75-per-cent compensation is most economic. Figure 11(C) shows the transmission costs of electric energy received on a 287-kv system for six parallel circuits and 600 miles in length. The most economic arrangement for this system is three intermediate switching stations with 75-per-cent compensation.

The cost of transmitting electric energy (excluding transformer costs) as a function of the per-cent series compensation is given for three typical system configurations on Figure 12. For 600-mile

Table II

Length of Line, Miles	Number of Parallel Circuits	Per-cent Series Compensation	Economic Number of Equally Spaced Intermediate Switching Stations	Spacing of Intermediate Switching Stations, Miles	Circuit Configuration Giving Representative Economic Power Limit
50 or less	2	0	0		*
100	2	0	1	50	*
300	2	0	5	50	
300	2	50	4	60	*
300	2	75	3	75	
600	2	50	More than 5	Less than 100	
600	2	75	5	100	*
600	6	0	5	100	
600	6	75	3	150	*

*The circuit configuration (number of switching stations and per-cent series compensation) which gives the representative economic power limits for each distance is indicated by asterisks in the last column.

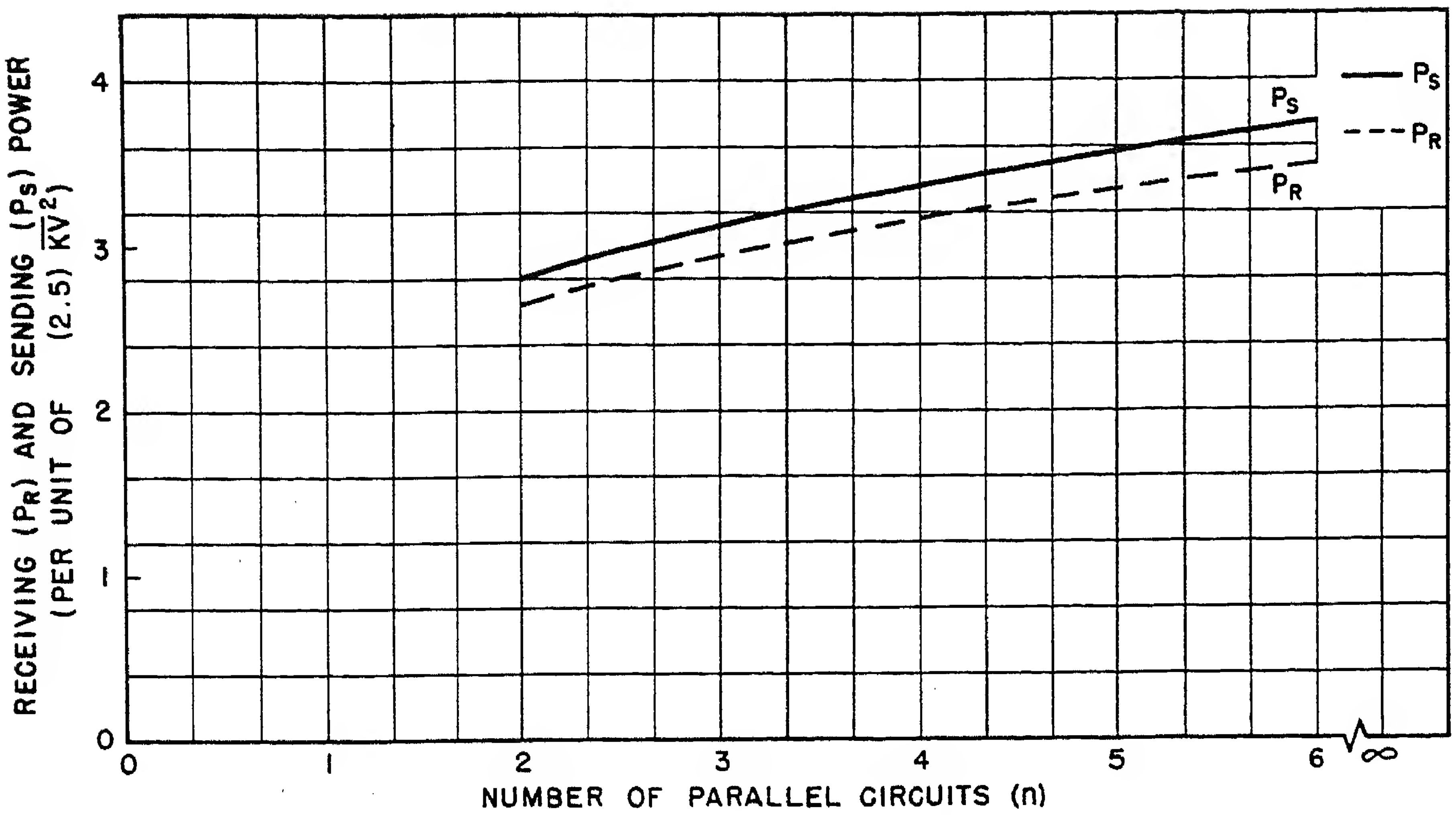
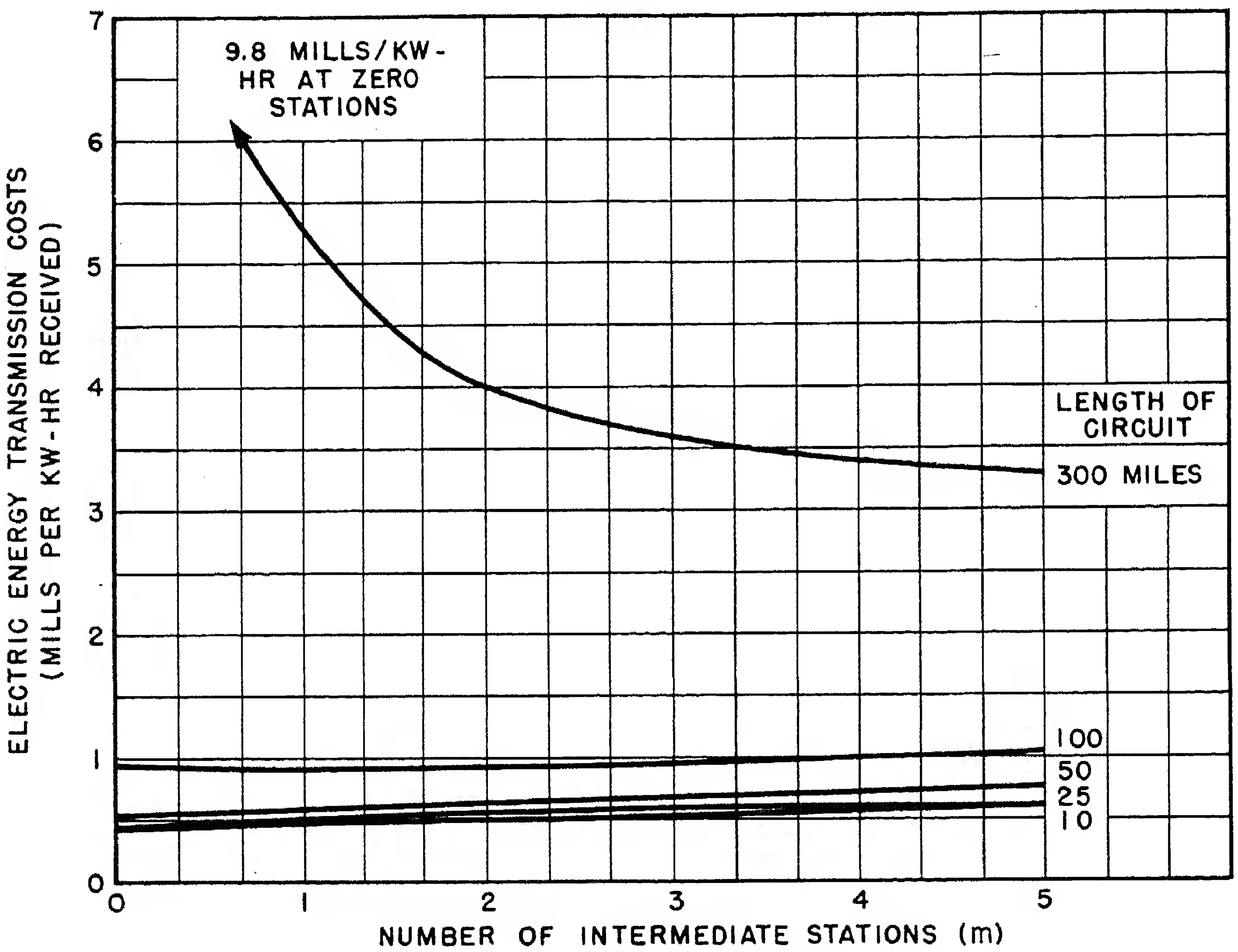
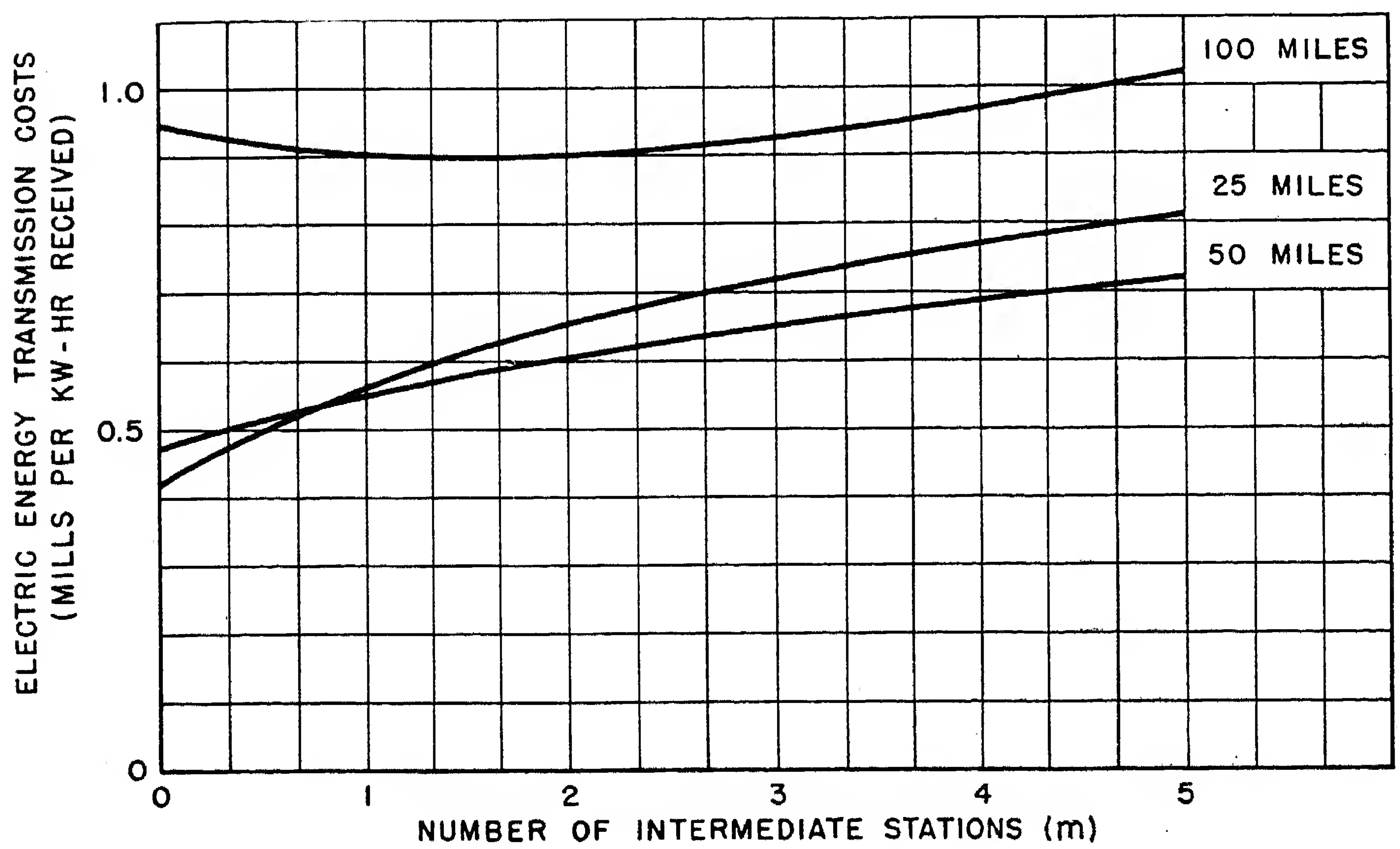


Figure 6. Effect of number of parallel circuits on transient stability limit per circuit for 25-mile line. Based on assumptions of Figure 2, Part A





lines with two or six parallel circuits and five and three intermediate switching stations, 75-per-cent series compensation is most economic. However, for the 300-mile line with two parallel circuits and five intermediate switching stations, 50-per-cent series compensation gives the most economic system design. Additional economic benefits might be realized if larger conductors were used on the series-compensated lines.

Representative Economic Power Limits

By representative economic power limit is meant the power received at the maximum stability limit which can be justified economically from a consideration of the cost of transmission line, switching stations, compensation, and real and reactive line losses. The representative power limit of a transmission line is a function of line length and receiving-system reactance. As shown in Figures 7(A) and 7(B), the circuit configuration (number of switching stations and per-cent series compensation) which gives the representative economic power limit for a particular distance is essentially independent of the range of receiving-end reactances [$x_m=0.25$ to $x_m=0.25/(P_s)$] considered in this study. The representative economic power limits are shown in Figure 1 as a function of line length for the range of receiving-end reactances considered. The point symbols (X, Δ, □, and ○) shown on these curves indicate the corresponding system configuration as designated in the caption of Figure 1.

In general, the economic number of switching stations for a given length line decreases as the series compensation and/or the number of parallel circuits increases, as illustrated in Table II. Also, for a given distance, as the number of intermediate switching stations increases the economic per cent of series compensation decreases.

Figure 7(A) (center). Effect of equally spaced intermediate switching stations on electric energy transmission costs (excluding step-up and step-down transformers) for two parallel 287-kv circuits with zero series compensation. Based on assumptions of Figure 2, Part A

Figure 7(B) (left). Effect of equally spaced intermediate switching stations on electric energy transmission costs (excluding step-up and step-down transformers) for two parallel 287-kv circuits with zero series compensation. Based on assumptions of Figure 2, Part B

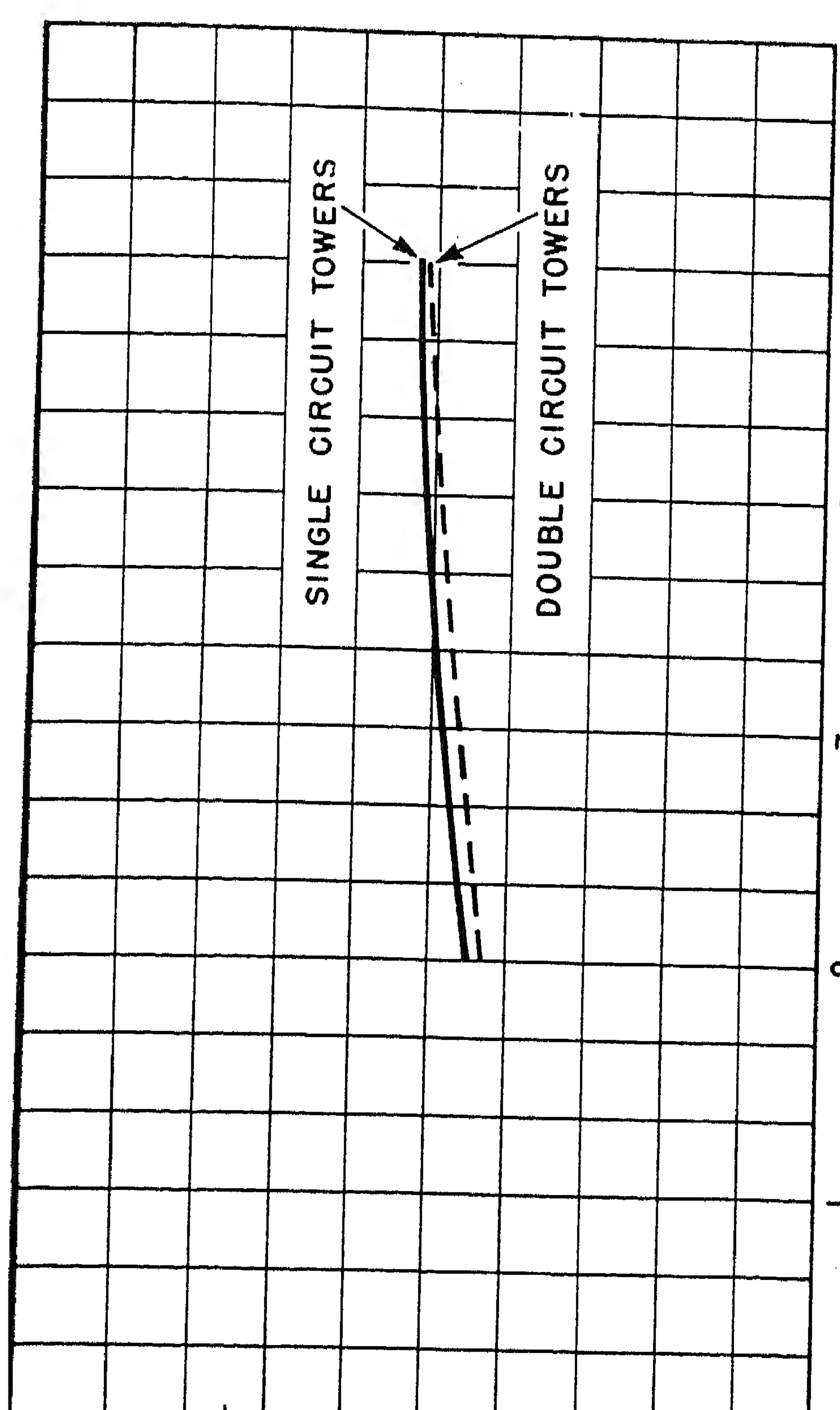


Figure 8. Effect of number of parallel circuits on electric energy transmission costs (excluding step-up and step-down transformers) for 25-mile 287-kv line. Based on assumptions of Figure 2, Part A

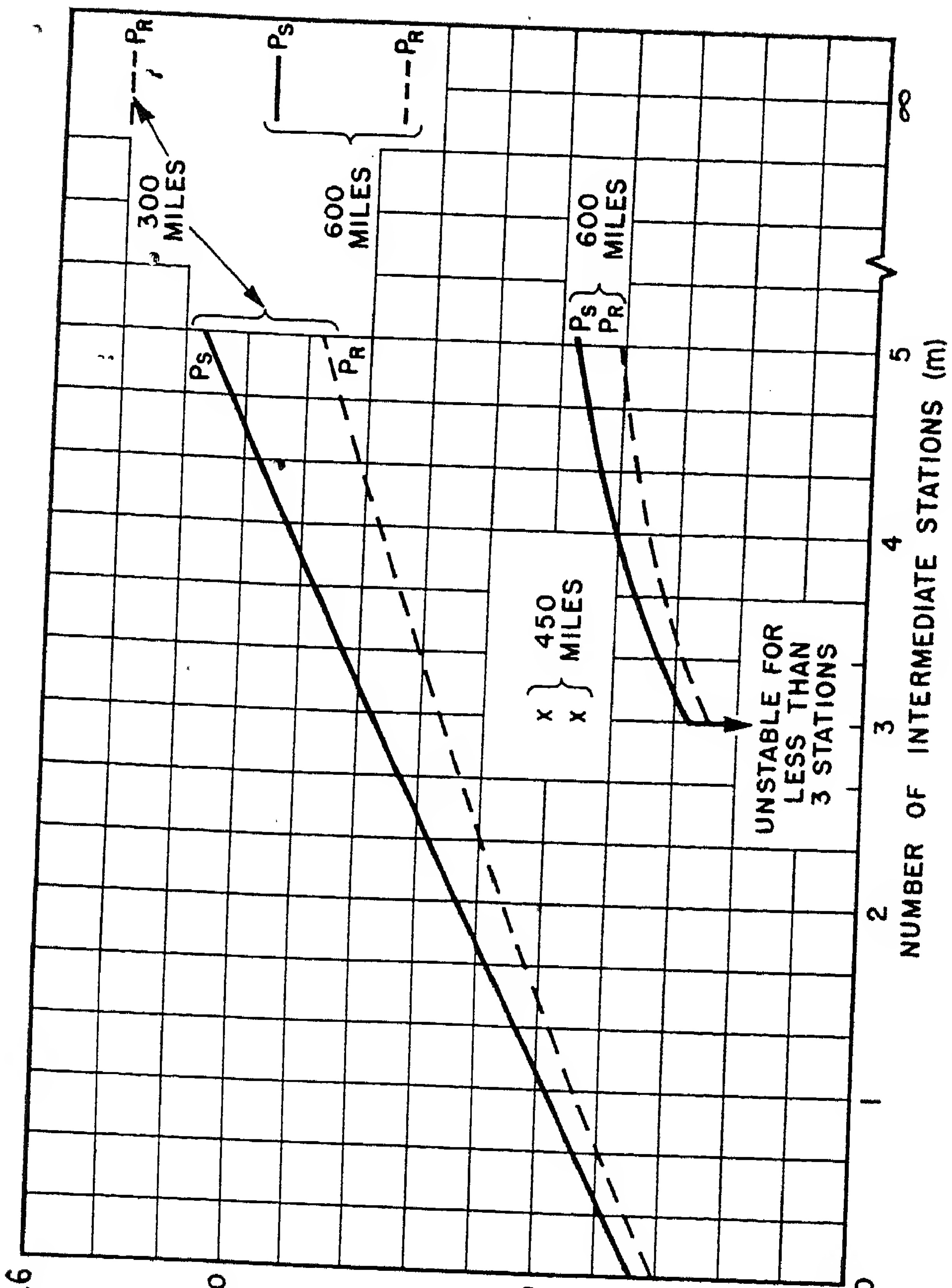


Figure 9(B). Effect of equally spaced intermediate switching stations on transient stability limit for two parallel circuits with 75-per-cent series compensation. Based on assumptions of Figure 2, Part A

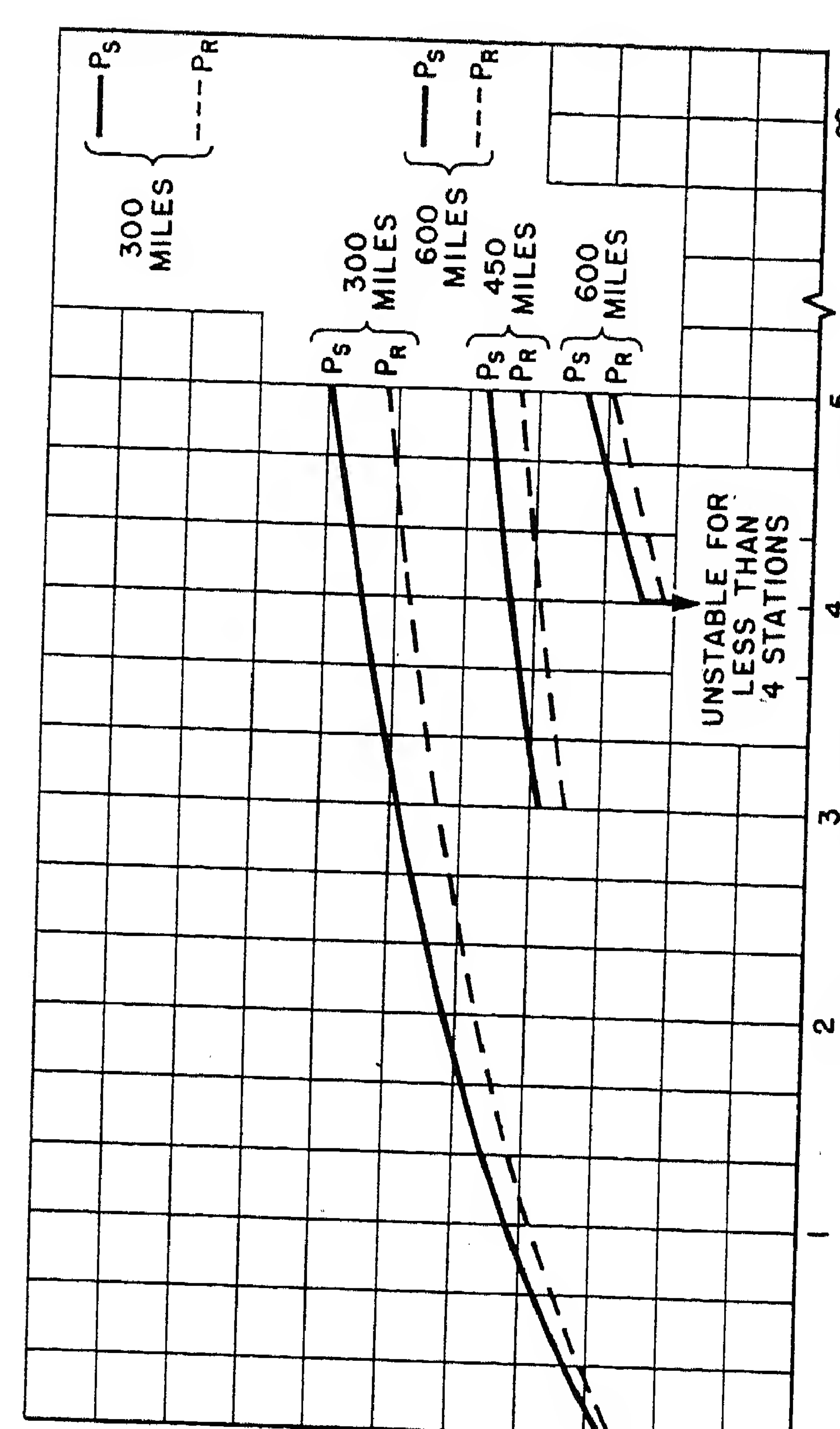


Figure 9(A). Effect of equally spaced intermediate switching stations on transient stability limit for two parallel circuits with 50-per-cent series compensation. Based on assumptions of Figure 2, Part A

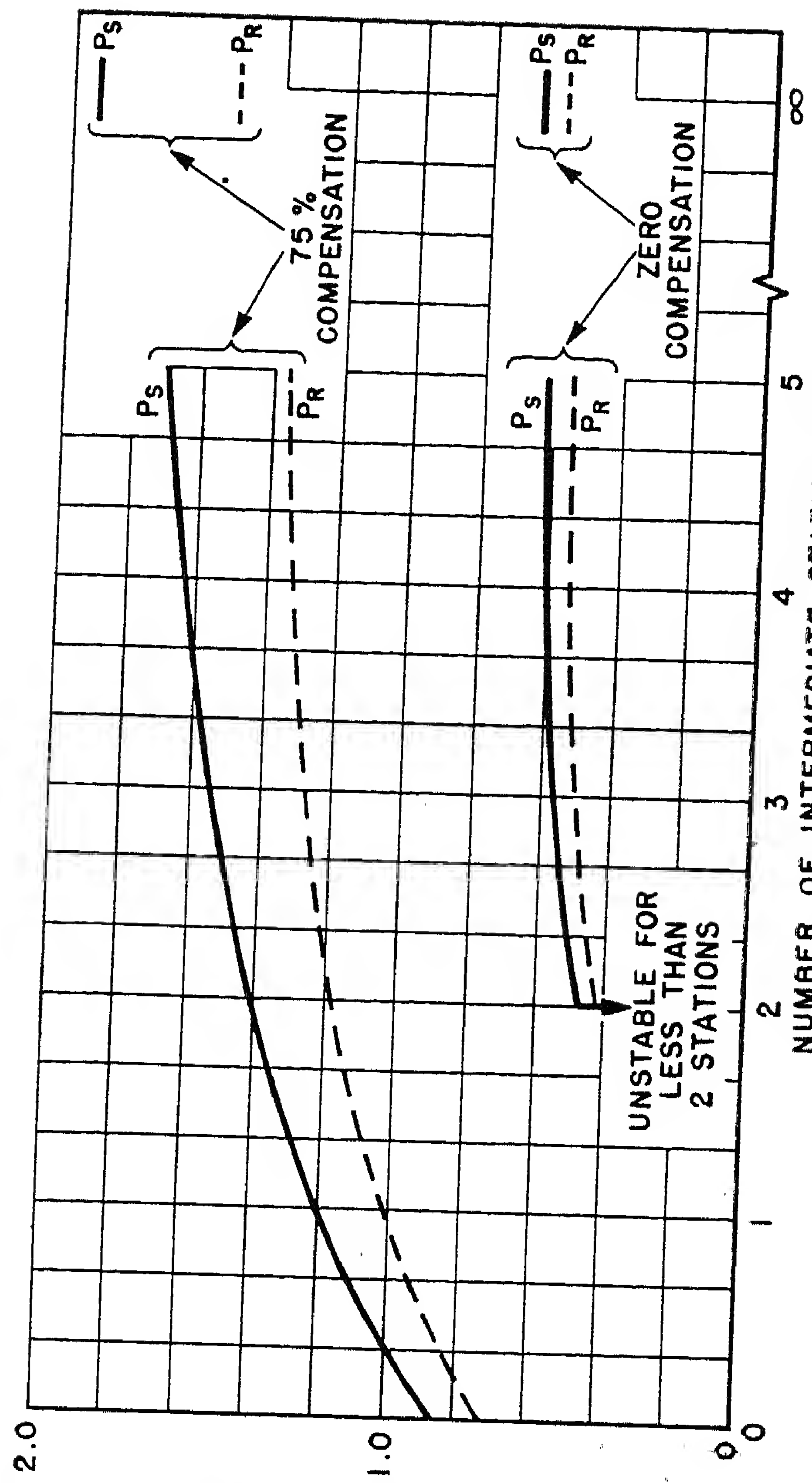


Figure 9(C). Effect of equally spaced intermediate switching stations on transient stability limit for six parallel circuits, 600 miles with zero- and 75-per-cent series compensation. Based on assumptions of Figure 2, Part A

Figure 10. Effect of per-cent series compensation on power received at stability limit for typical circuit configurations. Based on assumptions of Figure 2, Part A

NOTE: 600 miles—two parallel circuits with five stations are unstable for zero compensation

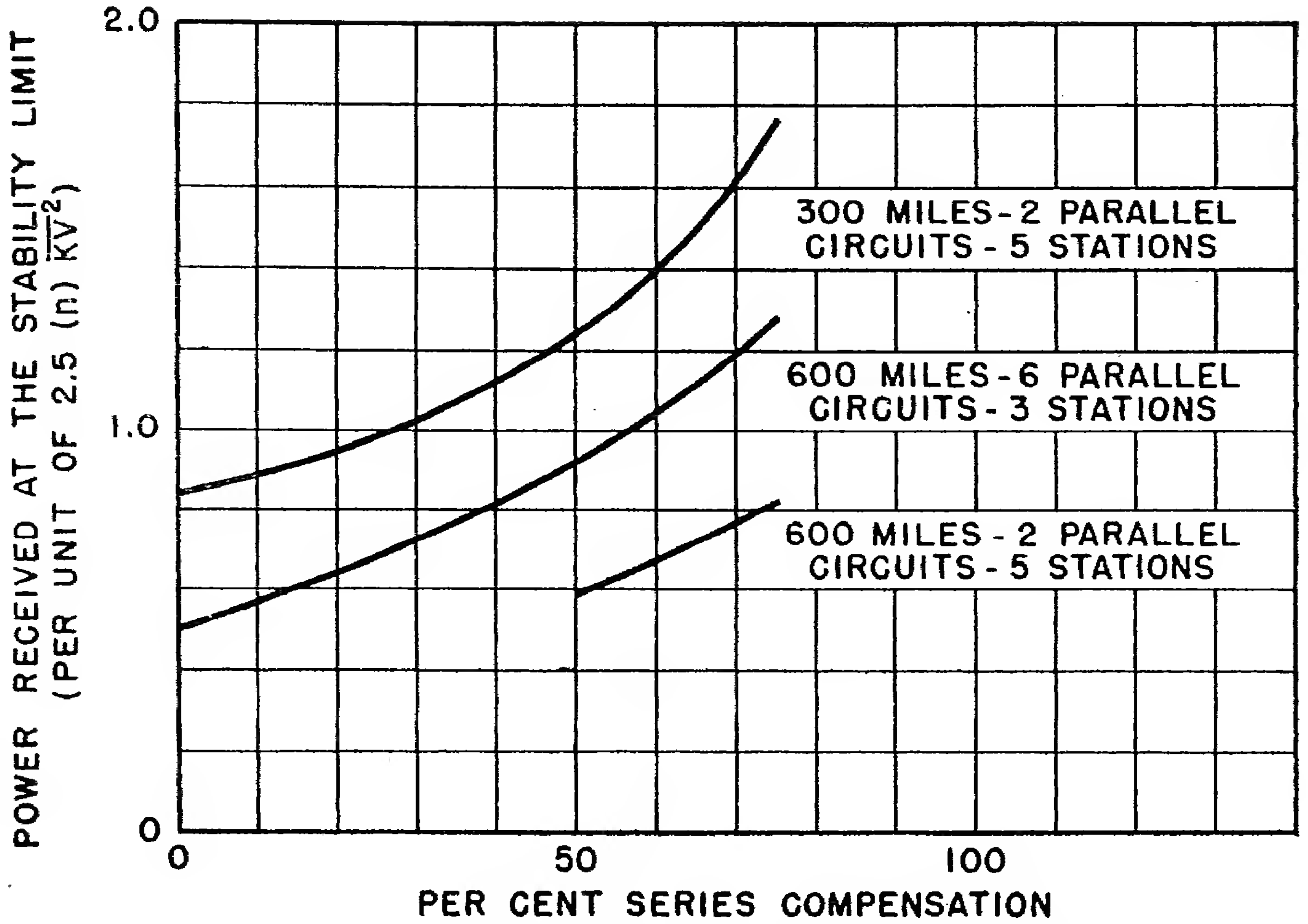
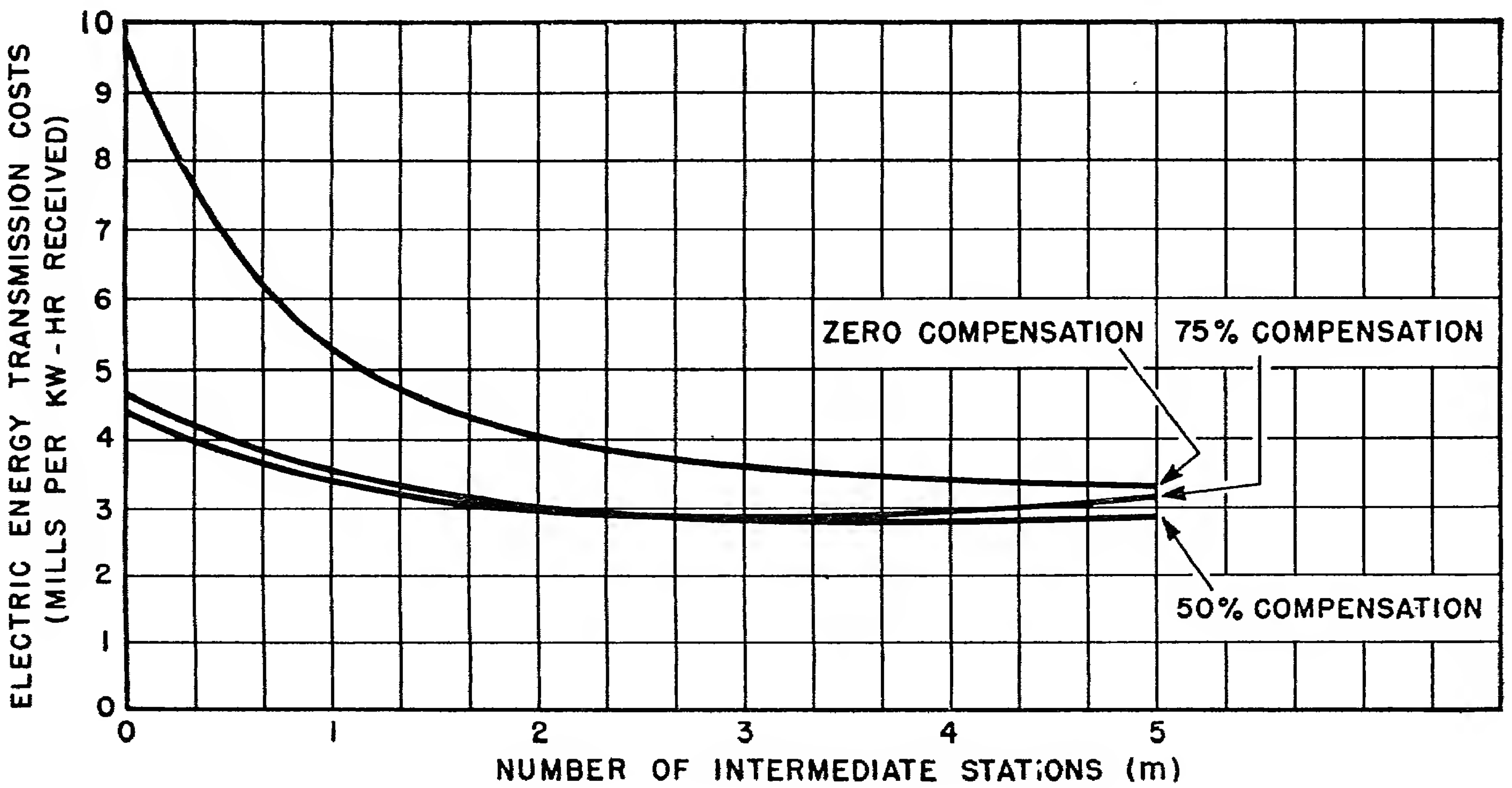
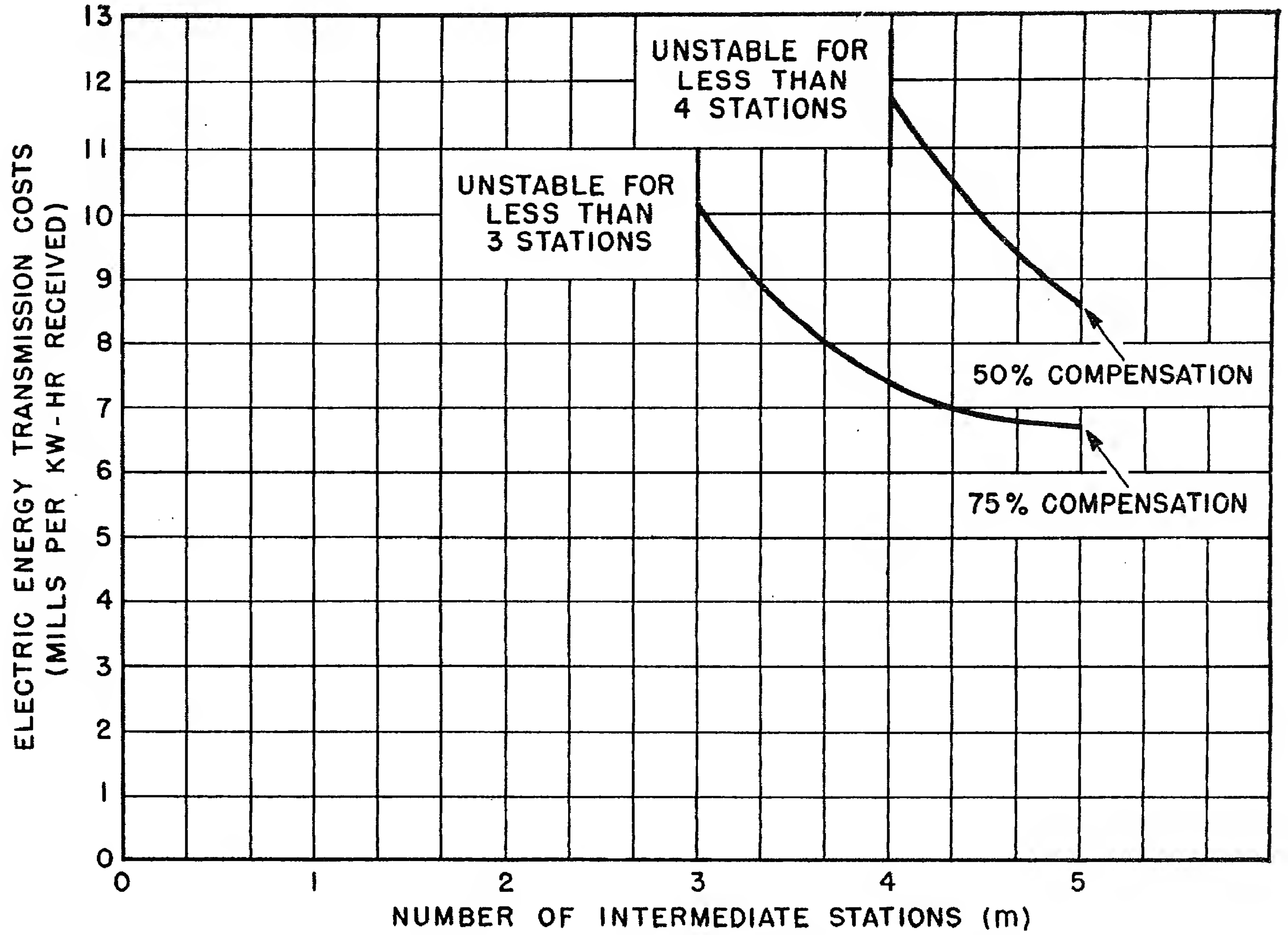


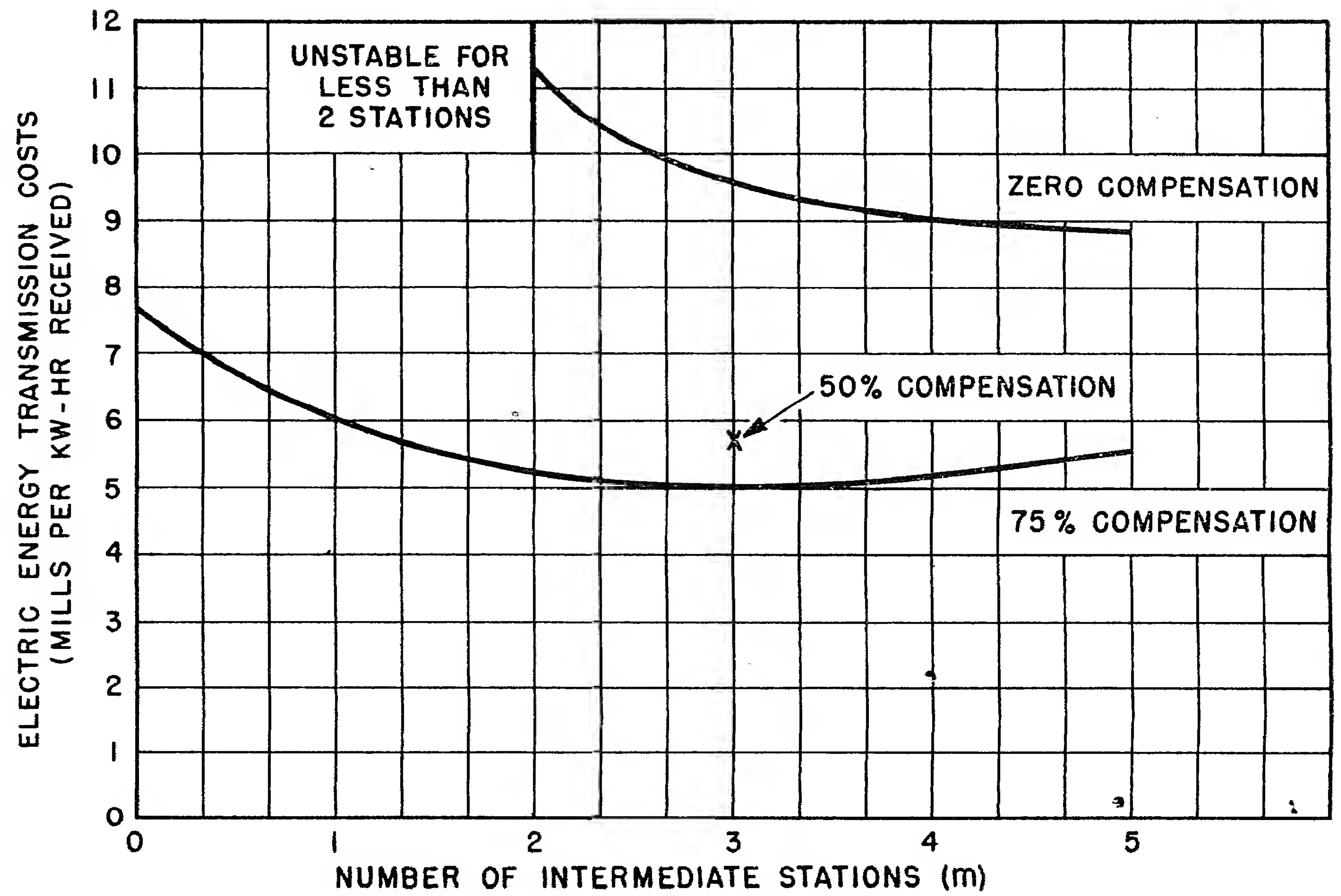
Figure 11(A) (below). Effect of equally spaced intermediate switching stations on electric energy transmission costs (excluding step-up and step-down transformers) for two parallel, 300-mile 287-kv circuits with zero-, 50-, and 75-per-cent series compensation. Based on assumptions of Figure 2, Part A

Figure 11(B) (right, top). Effect of equally spaced intermediate switching stations on electric energy transmission costs (excluding step-up and step-down transformers) for two parallel, 600-mile 287-kv circuits with 50- and 75-per-cent series compensation. Based on assumptions of Figure 2, Part A

Figure 11(C) (right, bottom). Effect of equally spaced intermediate switching stations on electric energy transmission costs (excluding step-up and step-down transformers) for six parallel, 600-mile 287-kv circuits with zero-, 50-, and 75-per-cent series compensation. Based on assumptions of Figure 2, Part A







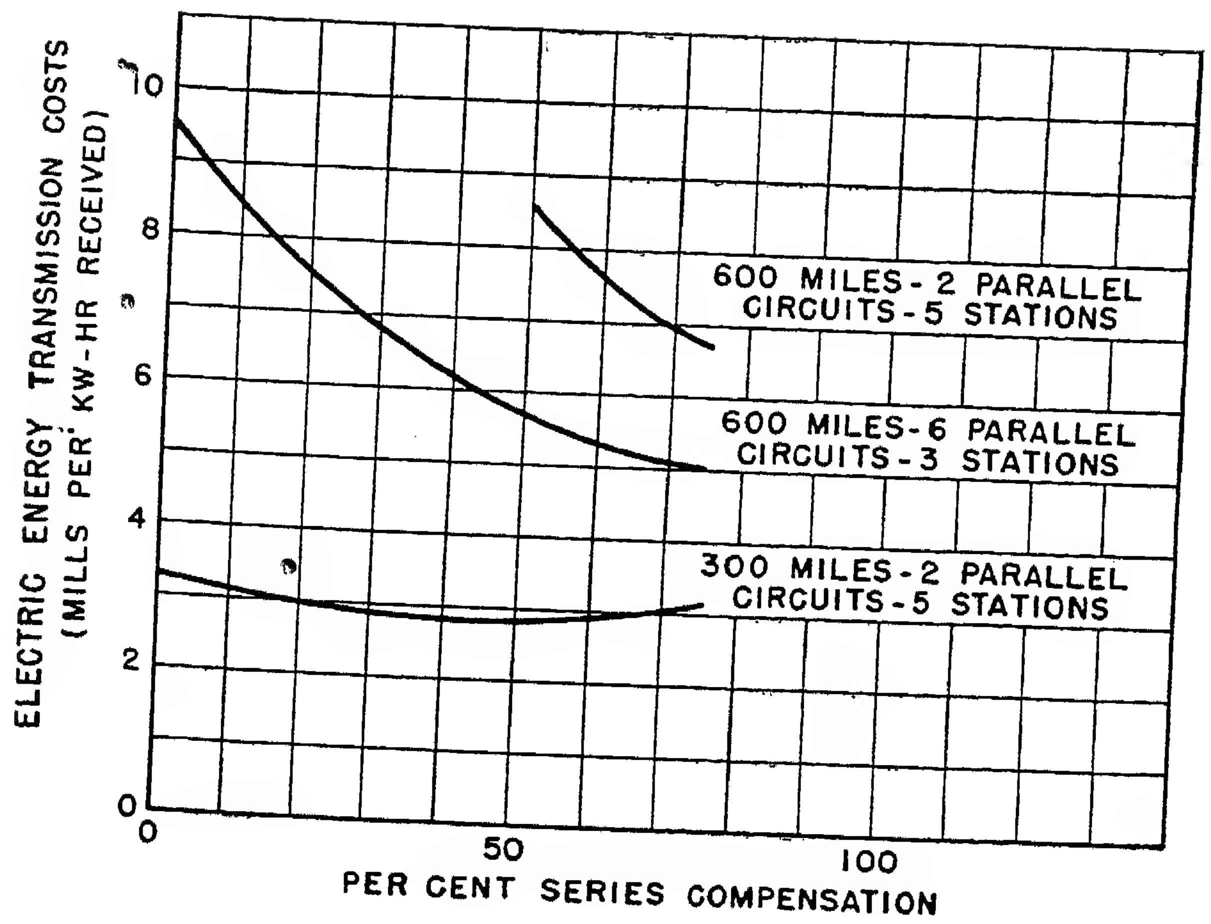


Figure 12. Effect of per-cent series compensation on electric energy transmission costs (excluding step-up and step-down transformers) for typical 287-kv system configurations. Based on assumptions of Figure 2, Part A

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Discussion

William R. Brownlee (Southern Services, Inc., Birmingham, Ala.): This analysis of the power limits of transmission lines is most timely because of the mounting interest in higher voltage transmission, especially the superposition of such higher voltages on existing transmission networks.

The element of timing has been omitted by the author in order to make a justifiable simplification of his basic results. However, the vital importance of this time element scarcely can be overemphasized in economic studies of various high-voltage systems. For example, consider a situation where a 115-kv line is required between two points which are in the path of a future 287-kv line. Would it be better to construct this part of the 287-kv line immediately and operate it at 115 kv for the intervening years on the basis that the 115-kv line, if built, would have little value following the completion of the 287-kv line? If the intervening period is as much as 5 or 6 years, it may be less costly (in terms of present worth of future carrying charges) to build the 115-kv line initially, even if it were considered worthless upon completion of the 287-kv line.

In the more representative areas of the United States, the intermediate switching stations are even more valuable than is indicated by the author's study, since they also provide convenient and economical locations for supplying intermediate load centers.

Stability limits of lines are of vital importance, since economical transmission at higher voltages depends so greatly on relatively heavy line loading. This is also true to a lesser extent in the case of medium voltage lines of larger conductors. Accordingly, in analyzing a proposed high voltage superposition on an existing network, comparisons should be based on economic loading of each system rather than on load limits for lines of the existing voltage based on "experience." Analyses show that many of these lower limiting values had their origin in system separations classed as "out of step," whereas the cascading of line outages actually was caused by the inability of relay schemes (and settings) on the unfaulted lines to distinguish between swing current and fault current. Engineers engaged in system planning should maintain close contact with operating engineers to assure that the full economies of high-voltage lines are realized.

E. E. George (Ebasco Services Inc., New York, N. Y.): Intermediate switching stations may be justified for reasons not covered by the author. One of the most common reasons is to connect with other power stations or major substations in an existing system. Long-distance straightaway bulk transmission is seldom required in this country.

Another point of interest concerns some of the author's charts on transmission cost. It appears that for all of the most economically attractive voltages, loads, and distances considered, the transmission cost is about 1 mil per kilowatt-hour per 100 miles. Apparently all the author's figures are for 50-per-cent load factor. Even if his costs are too high or too low, this constant unit transmission cost is sufficient to afford a convenient basis for preliminary study of long-distance transmission. It indicates that power cannot be transmitted economically to any great distance where fuel is cheap. In natural gas areas it may not pay to transmit hydro power to the nearest market under the usual conditions of stream flow and system load factor. In regions where fuel cost is very high, it may be justifiable to transmit hydro power several hundred miles.

G. D. Floyd (The Hydro-Electric Power Commission of Ontario, Toronto, Ontario Canada): I wish to raise two points covering this paper. These two points detract in no way from the value of the paper, which I consider is very timely and contains a great deal of useful information.

The first point has to do with the criterion for stability in a high-voltage system. In the studies described by the author, the power limit was determined by placing 3-phase faults at one of the generator busses. I suggest that this criterion is more severe than should be applied to a modern 3-phase power system, especially when the system is 230 kv or higher voltage. These high-voltage systems have been developed to the point where faults of this nature are extremely rare. It seems to me that the criterion for stability should be reviewed in the light of present-day design and a less severe criterion be established. I suggest that, if it is found compatible with good design to use as the yardstick a less severe fault condition, very appreciable savings will result. It may be that the incidence of lightning outages would dictate whether or not the present criterion or the new one would be the one to use in any given study. Due to circumstances beyond our control, it has been necessary on one of our 230-kv systems to operate the system during the last 2 years very close to the static stability limit. This system is designed for transmission of approximately 600 megawatts and the transmission design was a conventional one, using interswitching stations where required and limiting the loading per circuit to approximately the surge impedance loading. Our experience, while operating this system at close to the static stability limit, has been so good that it suggested a possibility of making this a normal method of operation. It does require more vigilance on the part of the operating staff. It has been found that close attention to the performance of the system, while operating at large load angles, permitted this method of operation. The first indication of approaching instability is a periodic variation in voltage which can be

observed. We also have used a load angle indicator to give a continual indication of the total angle, including the generator internal angle. Where the incidence of lightning is not too severe and during a large part of the year when there are no line faults due to lightning, it seems quite permissible to operate a system in this way.

The second point questions some of the cost data shown in Figure 2 of the paper. My understanding of Figure 2 is that the author used only a single high-voltage circuit breaker per line for the system study. A system handling the powers given in the study would require for service security a more complex switching arrangement than shown. This would, of course, affect the economic picture. Some of the unit costs appear to be unrealistic. As an example, the cost of $10 per kilovar for the receiving-end compensation is lower than present-day costs for the installed cost of synchronous condensers. The unit costs for single-circuit and double-circuit steel tower lines, if these are of conventional design, are lower than present-day costs. The conclusions arrived at may therefore be out of line. In questioning these costs, it is only to be pointed out that the economic comparisons derived therefrom may not apply in all cases. The value of the analysis, however, is still very high as it points out the methods which can be used in making such an analysis. Anyone interested can use the methods and substitute his own known costs, deriving from his analysis the proper conclusion to be drawn for the case which he is studying.

H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): In carrying out this very extensive transmission study the author has held to a single criterion of power limit, namely, the maximum stability limit. The results described are interesting and valuable and certainly for the longer lines studied the limits obtained are entirely practical and could be approached in service, allowing for some margin of safety below the theoretical limit.

On the other hand, when shorter lines are considered, such as 100 miles and below, it does not seem at all correct to define power limits of transmission lines solely in terms of the maximum stability limit. For these shorter lines there are other limits such as thermal capacity of conductors and reactive losses. Theoretically, these limitations can be taken care of by using larger conductors and installing reactive correction, but there are some reasonable and practical limitations to both of these remedies.

Specifically, it seems to me that the author's Figure 1, which summarizes his entire conclusions, can be very misleading because of his strict adherence to the single criterion of maximum stability limit. There is no recognition of any other practical limitations for the shorter distances, 100 miles and below, although the author does refer briefly to such limitations in other parts of the paper.

As an example, under the assumption of one-line section out of service, the 100-mile line with two circuits and one intermediate switching station would have a limit, according to Figure 1, of 720 megawatts at 287 kv, and all of this, representing a current of 1,450 amperes, would have to be carried over the remaining 50-mile single-circuit section. While the assumed conductor of 800,000-circular-mil copper equivalent might carry this current for a short time at least, the reactive loss represented by 3.5 times surge-impedance loading in the 50-mile single-circuit section would be quite high, about 235,000 kilovars, or a total loss for the entire line of 350,000 kilovars.

Referring again to Figure 1 of the paper, the power limit for a 50-mile line on the basis of one of two parallel circuits out of service is given as 1,030,000 kw at 287, kv which represents a current of 2,070 amperes. This current is well beyond the thermal capacity of 800,000-circular-mil copper equivalent and the reactive loss in this case would be approximately 500,000 kilovars which is a large loss for a 50-mile line.

Although the author's Figure 1 is labelled "Representative Economic Power Limits for Transmission Circuits," it is not my understanding that either the conductor requirements or the cost of this excessive correction were included in the analysis.

Practical limitations of this character were taken into account in setting up the basic capability figures for the economic study which E. L. Peterson and I carried out.[1]

From practical considerations, it might be in order also to question the author's conclusions in regard to economics of intermediate switching stations, particularly for parallel lines as short as 60 miles. Figure 7(A) of the paper, for example, shows only a slightly greater economy resulting from the use of one switching station in two parallel 100-mile circuits. Aside from practical limitations of conductor size and reactive losses, even the small economy indicated in Figure 7(A) would disappear completely if and when one or more circuits were added to such a line. In the studies described by St. Clair and Peterson,[1] the number of switching stations assumed was held somewhat below the point of maximum economy in the expectation that circuits will be added in the course of normal system growth.

Also I would be inclined to question the practical wisdom of intermediate switching stations every 60 miles on a 300-mile 2-circuit line. Even with 50-per-cent series compensation, the increased economy of going from three to four intermediate stations would seem to be relatively small, and again would be lost entirely if a third circuit were added.

Finally, I wish to commend the author for carrying out such an exhaustive study and for presenting the results in compact form in this paper. At the same time, I believe the value of the paper would have been considerably enhanced if the theoretical power limit curves shown in Figure 1 of the paper had been supplemented by similar curves showing practical limits, taking into account the limitations for shorter lines and including some reasonable margin of safety for all of the other lines.

REFERENCE

1. See reference 5 of the paper.

Lindon E. Saline: The discussors have brought out several interesting points which require only brief comments.

Mr. Brownlee has properly re-emphasized the importance of considering "time" as a factor in economic studies. Basically the problem is that of evaluating how much excess capacity can be provided during the period of load growth in order to obtain the economic benefits realizable with high voltages (and heavier line loadings) after the ultimate line loading is reached. Certain investments such as additional lines, switching stations, or series compensation for the longer lines can be deferred, and these possible steps in building a high-voltage system can and should be evaluated before selecting the level of a high-voltage system. Such an evaluation along with other practical considerations can serve as a useful guide in power system planning.

Mr. Brownlee and Mr. George have mentioned the value of intermediate switching stations as points to tap off intermediate loads or to interconnect with other power systems. Hence, in addition to increasing the economic line loading for straightaway distances greater than about 60 miles, intermediate switching stations may be considered as necessary in over-all system planning as load centers or points of interconnection. Other benefits of connecting with intermediate systems are given in reference 1 of the paper.

Mr. Floyd has questioned the use of a 3-phase fault on the generator bus as a transient stability criterion and he has suggested that additional economies might be realized by using a less severe fault condition. However, for quick switching times the transient stability limit is determined primarily by the change in impedance resulting from switching a line section rather than by the type or location of the fault. The effect of various fault conditions on the transient stability limits of typical systems is "Power System Stability—Volume II."[1] The location and type of fault for the systems shown in this reference are relatively unimportant in determining the transient stability limit of the system for quick switching times. As the inertia of the systems increases, the location and type of fault for quick switching become even less significant. Hence, for the system and quick switching times ($t=0.05$ second) studied in this paper, the increase in transient stability limit would be inconsequential if, for example, a line-to-ground fault were used rather than a 3-phase fault.

With regard to the arrangement of the switching stations the author agrees with Mr. Floyd that more circuit breakers are usually required to give the necessary "service security." If it is deemed advisable to use more switching stations, the economic studies can be modified to include the additional costs and the results would tend to be less favorable (but only in small degree) toward the use of intermediate switching stations. The line costs could also be modified as desired to show the effect of increased line costs as proposed by Mr. Floyd. In general, reasonable variations in the assumed costs would not affect the basic conclusions to any marked degree. The large expenditures of money involved in the design and building of transmission systems warrant individual consideration and study which includes the cost assumptions and circumstances applicable to each particular case. The intent of the present study is to indicate certain general trends in transmis-

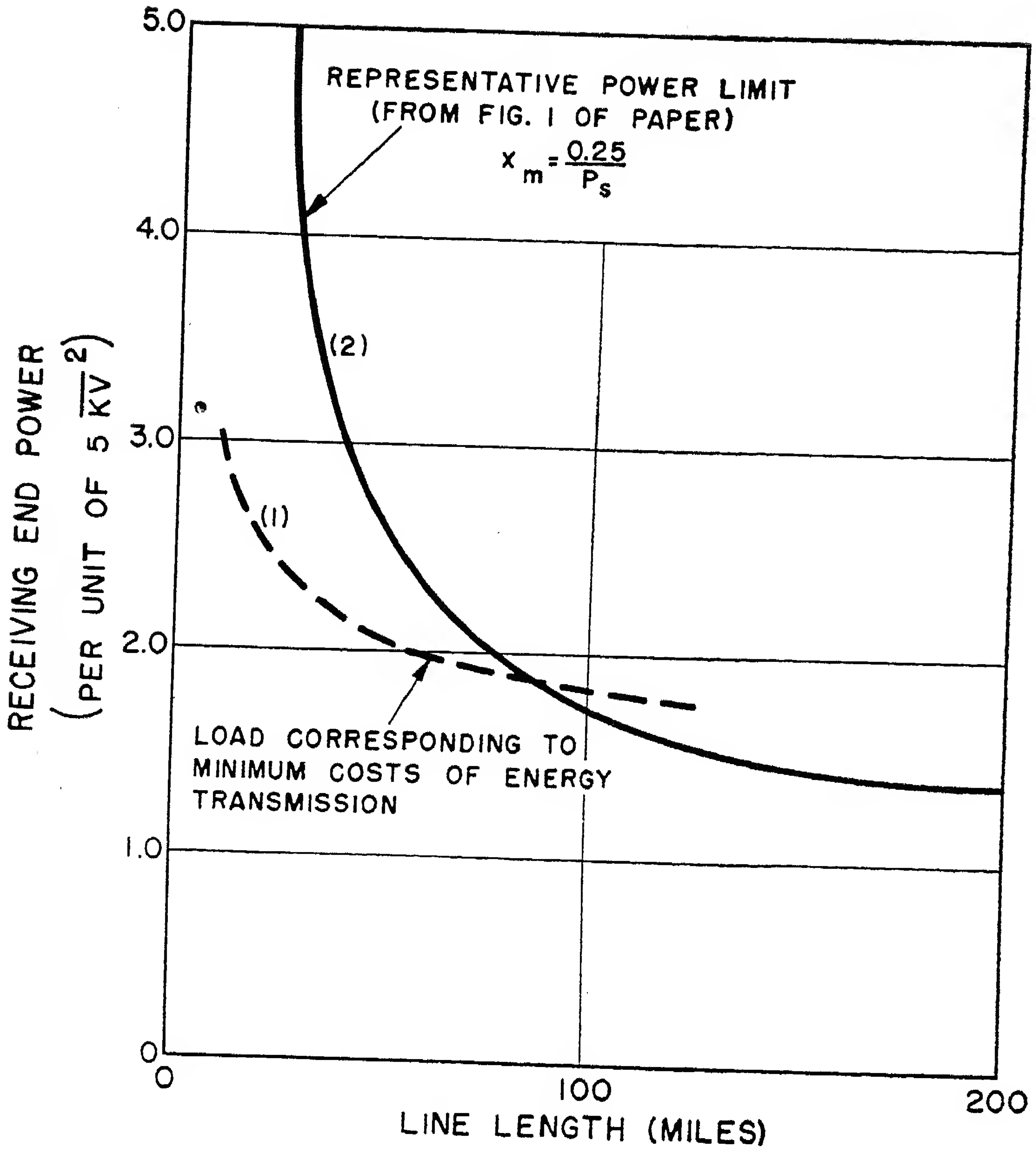

Figure 1. Comparison of representative power limit with the load corresponding to minimum costs of energy transmission as a function of distance for two parallel, high-voltage transmission circuits (based on cost data for 287-kv system)

sion system design rather than to present a study relating to a special case.

The comments presented by Mr. St. Clair are of interest and indicate the need for a more detailed explanation of representative economic power limit as used in Figure 1 of the paper. As stated in the paper, "By representative economic power limit is meant the power received at the maximum stability limit which can be justified economically from a consideration of the cost of transmission line, switching stations, compensation, and real and reactive line losses." The meaning of representative economic power limit is further illustrated in Figure 1 of the discussion which shows two curves:

1. The load corresponding to the minimum cost of energy transmission at each distance.
2. The representative power limit (based on power transferred at stability limit for the same system configurations as used for curve 1).

Curve 1 corresponds to the system loading which gives minimum costs for energy transmission for the same system configurations indicated in Figure 1 of the paper. Curve 2 corresponds to the maximum stability limit of the system for the same system configurations as used for curve 1. Over a range of line lengths from 10 to 600 miles the economic loading curve is composed of the parts of two curves: one part (at long distances) determined by the transient stability limit and another part (at short distances) determined by using the load corresponding to the minimum cost of energy transmission. The economic loading curve for the illustration in Figure 1 of the discussion is curve 1 from 10 to 90 miles and curve 2 from 90 to 200 miles. For the particular conditions illustrated here it is evident that the stability limit of the system is the controlling factor for distances greater than 90 miles, but that the line loading is limited by only the real and reactive losses at distances less than 90 miles.

With regard to Mr. St. Clair's remarks about the decreasing economy of switching stations as the number of parallel circuits increases, the author is in full accord as stated in item 4 of the section in the paper entitled "Conclusions." As is Mr. St. Clair's understanding, the author did not use the emergency reactive requirements to maintain unit voltage at the receiving end bus as a condition for the economic study. Included in the cost analysis were the reactive requirements for the normal operating condition which is thought to be a rational condition on which to base the cost studies.

This paper does not include a study of conductor economics, but, as Mr. St. Clair points out, the line loading also might be limited by thermal capacity. This is particularly true at short distances if the power transmitted over the line remaining in service during long-time emergency conditions is the same as the power transmitted over two lines during normal operation. For example, using the rather severe criterion of loading by thermal capacity under emergency conditions suggested by Mr. St. Clair, the maximum line loading would be limited to approximately 2 per unit (10 kv^2). Hence, applying this criterion to the illustration shown in Figure 1 of the discussion, the loading curve would be composed of three parts:

10 to 60 Miles (approximately 2 per unit as determined by thermal capacity during emergency conditions)
60 to 90 Miles (curve 1)
90 to 200 Miles (curve 2)

Limitations due to thermal capacity can be overcome economically in many cases by using larger size conductors.

Reference

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A New 14.4-Kv Indoor Compressed-Air Circuit Breaker

J. E. SCHRAMECK
MEMBER AIEE

In 1939–40, soon after compressed-air circuit breakers were first introduced in this country, interest in these circuit breakers by central station users was high, and it remained so. European designs for limited interrupting requirements had preceded American designs. This was a natural outgrowth of necessity dictated by the scarcity of oil and its high cost in Europe. The use of oil in circuit interrupters in this country presented no obstacle. Its use was efficient, improved interrupters had been developed, and American industry with a large investment in tooling and know-how recognized that an immediate change from oil to air was not necessary. However, there were obvious advantages in the use of oilless circuit breakers for indoor service. Principal of these is the fire and smoke hazard in the case of failure in the indoor circuit breaker, which the operators wished to eliminate. American industry answered this demand and the indoor compressed-air circuit breaker became a reality.

Much has been learned in the intervening years. Thousands of these circuit

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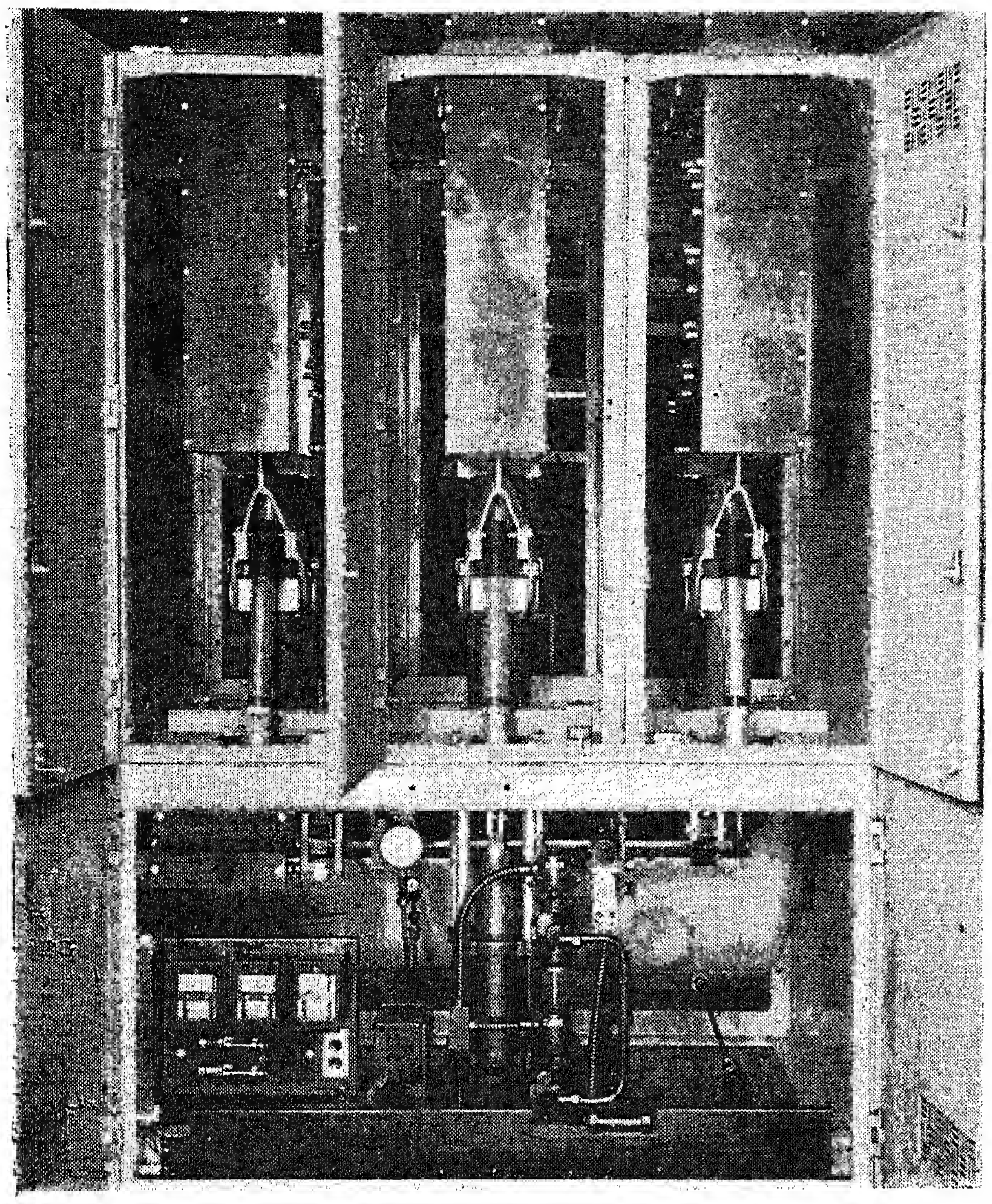


Figure 1 (left). New design of 1,500,000 - kva 14.4-kv 2,000-ampere compressed-air circuit breaker

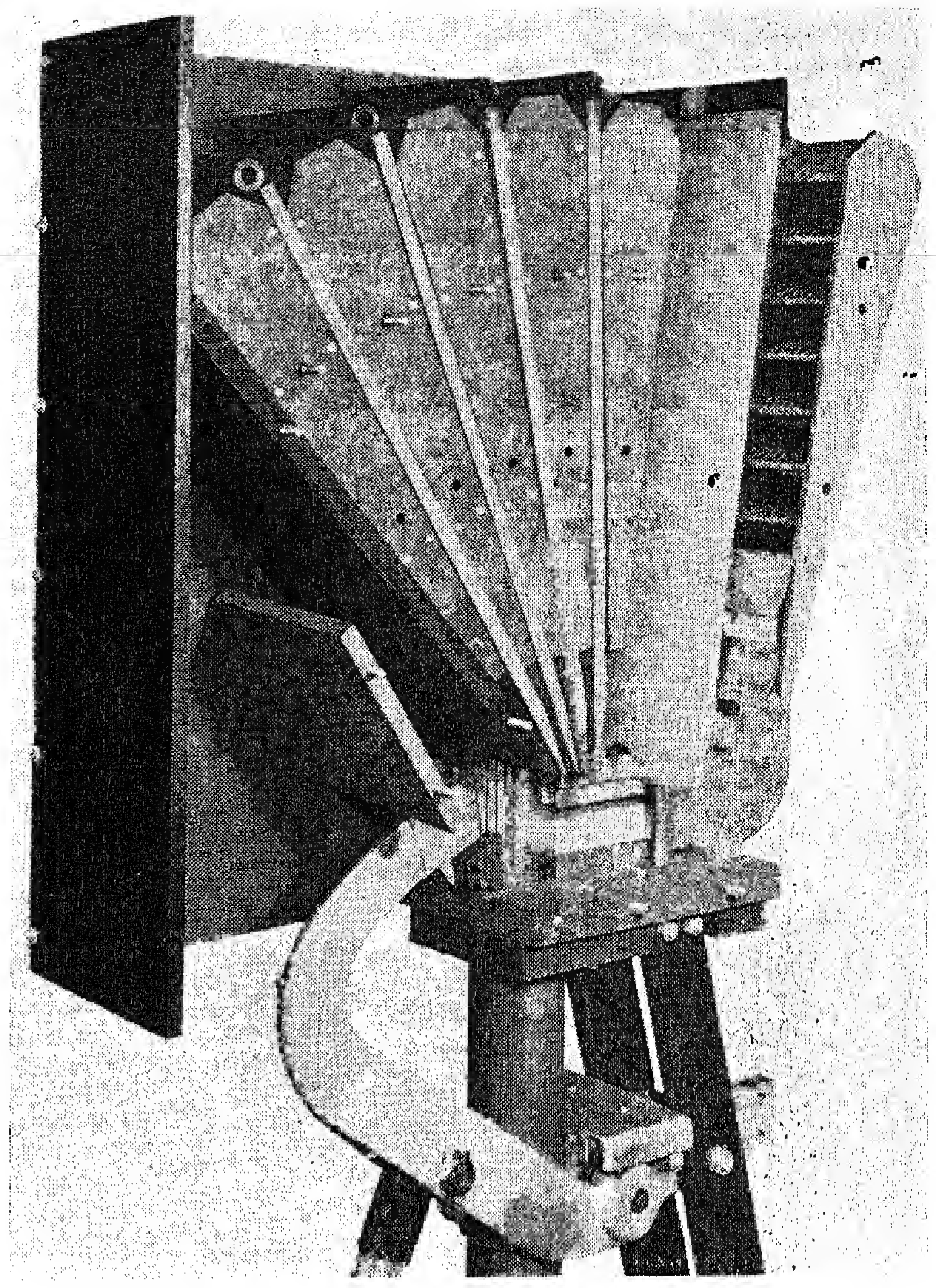


Figure 3 (right). 1,500,000 - kva arc chute with one side removed

breakers have been manufactured. The resulting evolution has brought forth many significant changes in design concepts. The designer realized all too soon that previously developed ideas relating to the use of compressed air in air brakes, injectors, and the like, could not be applied to meet the exacting requirement of circuit breakers, unless certain changes were made. It was necessary to develop new application techniques before the compressed-air circuit breaker could be the dependable circuit interrupter that it is today. Rather than present the details of these techniques, it is the writer's intent to present the end results and their use in a circuit breaker.

Figure 1 shows a modern compressed-air circuit breaker having an interrupting rating of 1,500,000 kva at 14.4 kv with a continuous current rating of 2,000 amperes. This circuit breaker features reduced space requirements over the same design rating of 10 years ago. For instance, the circuit breaker width has been reduced from 81 inches to 62 inches. In the 4,000-ampere rating the width is reduced from 81 inches to 68 inches. A reduction of weight from approximately 6,000 pounds to 4,200 pounds has been obtained. The savings in required building costs is obvious. Savings in critical materials also resulted. These gains were not made at the expense of insulation. The 110-kv impulse level has been retained. Design changes in the contact members utilizing a greater percentage of cast cupaloy and new pivotal bridging members resulted in reduced pole unit width. Improved interrupter assemblies have permitted a reduction in arc chute width of 22 per cent, the adequacy thereof having been demonstrated by hundreds of high-power laboratory tests, many exceeding the maximum current interruption obtained 10 years ago.

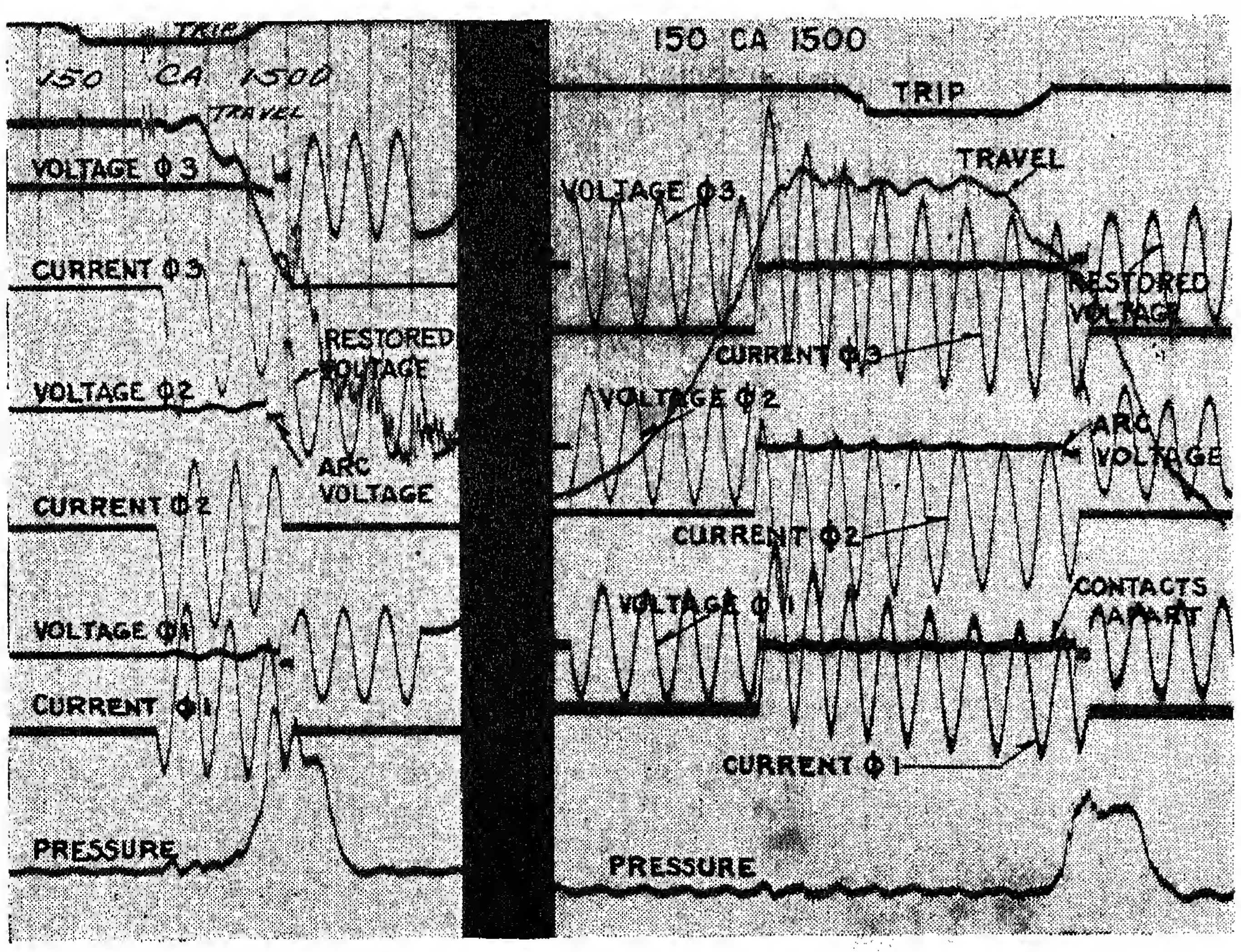


Figure 2. Typical oscillograms showing high-power laboratory tests of closing and opening operations on a 1,500,000-kv 14.4-kv compressed-air circuit breaker

The requirements that this circuit breaker interrupt 72,000 amperes at 12,000 volts and to close against its momentary rating of 115,000 amperes has been fully demonstrated by typical high-power laboratory tests, oscillograms of which are shown in Figure 2. The oscillogram on the left shows interruption of 70,000, 68,000 and 78,500 rms amperes on phases 1, 2, and 3 respectively for a 3-phase initial line-to-line voltage of 13,200 volts. The oscillogram on the right shows a close-open duty cycle wherein the circuit breaker closed 139,000, 76,000, and 125,000, and interrupted 61,000, 58,000, and 60,000 rms amperes on phases 1, 2, and 3 respectively with an initial line-to-line voltage of 13,200 volts.

The general circuit breaker arrangement has remained essentially unchanged. The high-voltage compartment is separated from the mechanism compartment, containing the circuit breaker control, by grounded metal plates as protection to personnel. The operating pressure at 150 pounds per square inch and straight line air flow from the blast valves up to the arc chute have been retained.

In the mechanism compartment new improved shock absorbers have been pro-

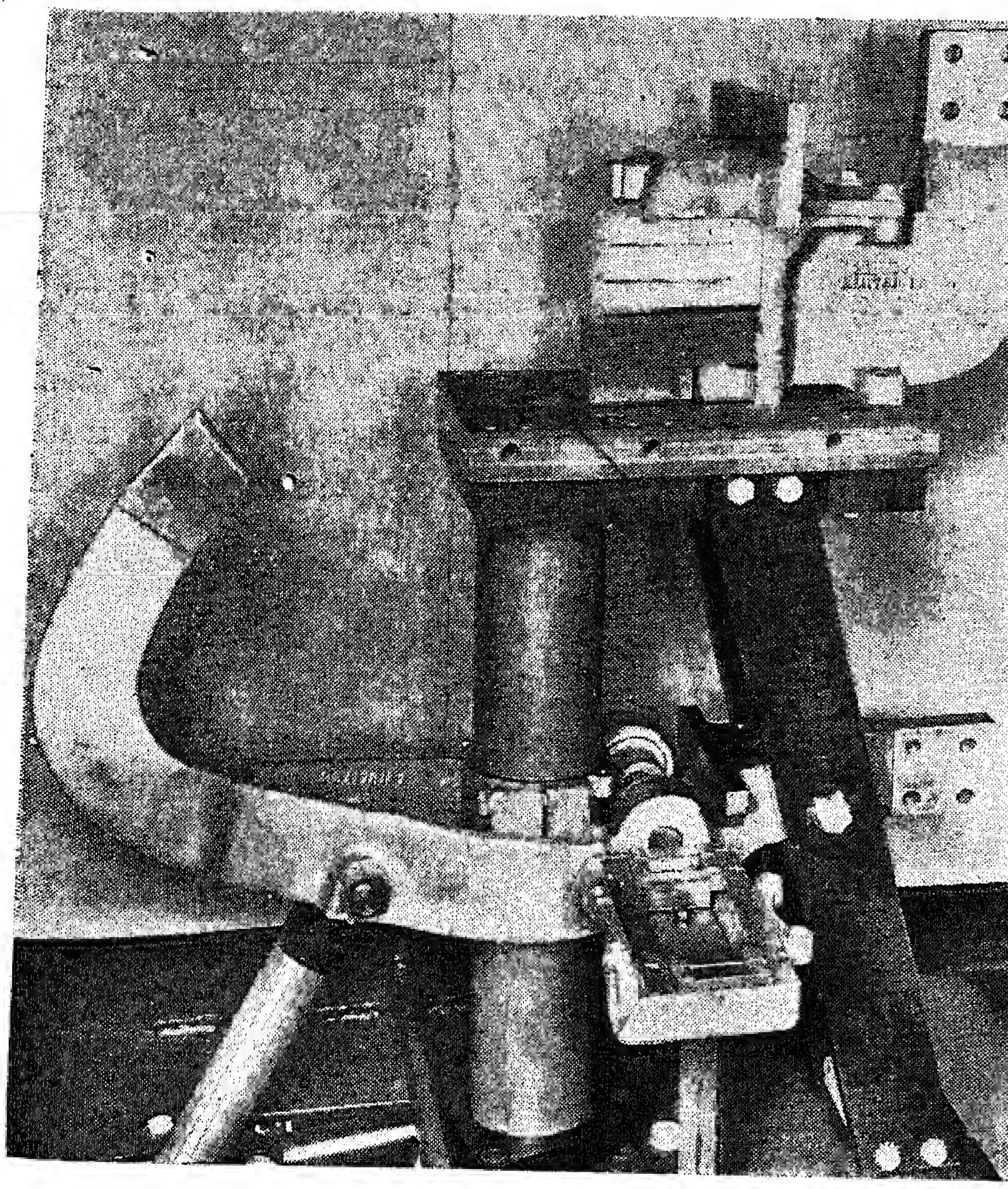


Figure 4 (left). 2,000 - ampere 1,500,000 - kva pole unit

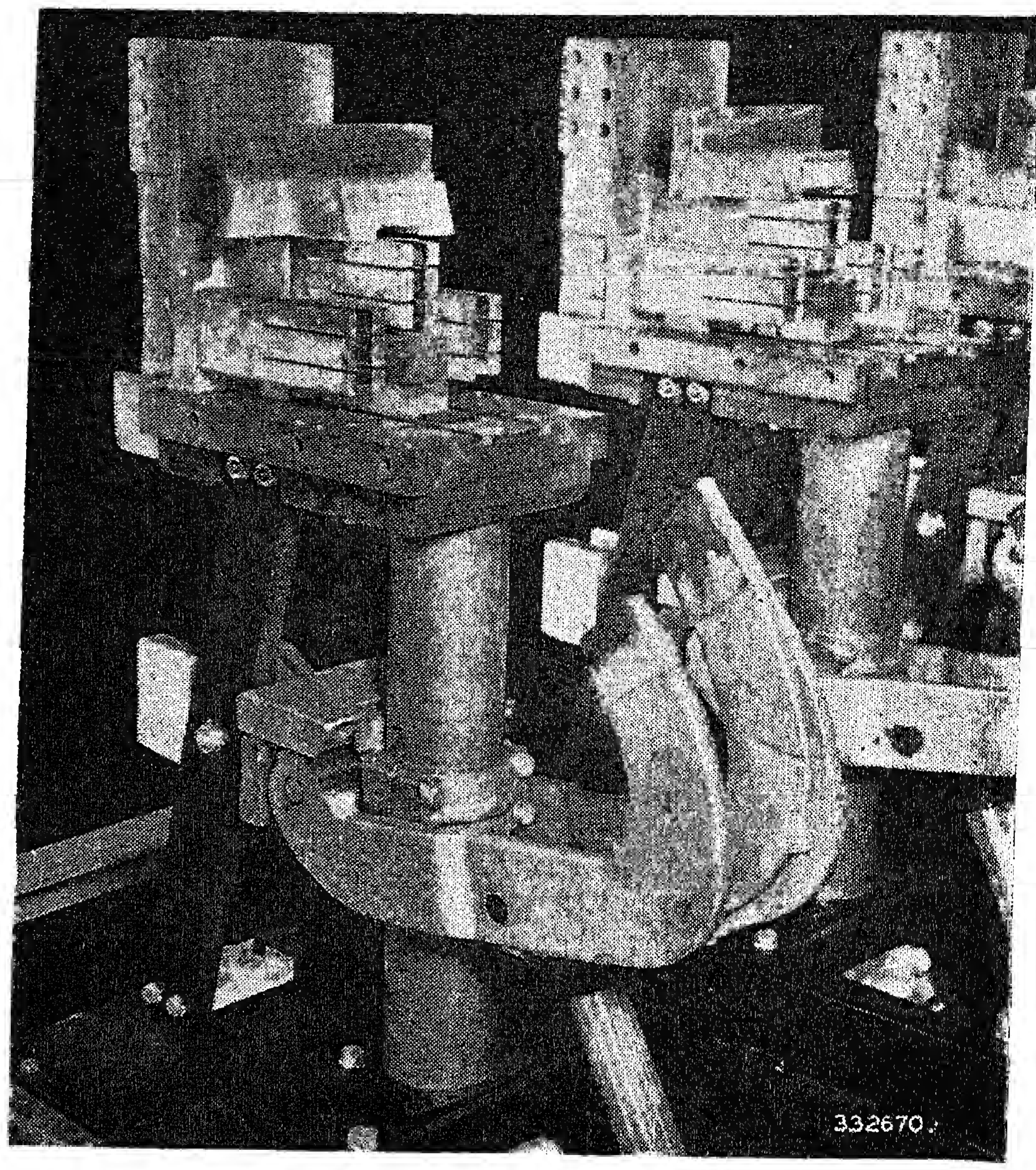


Figure 5 (right). 4,000 - ampere 1,500,000 - kva pole unit

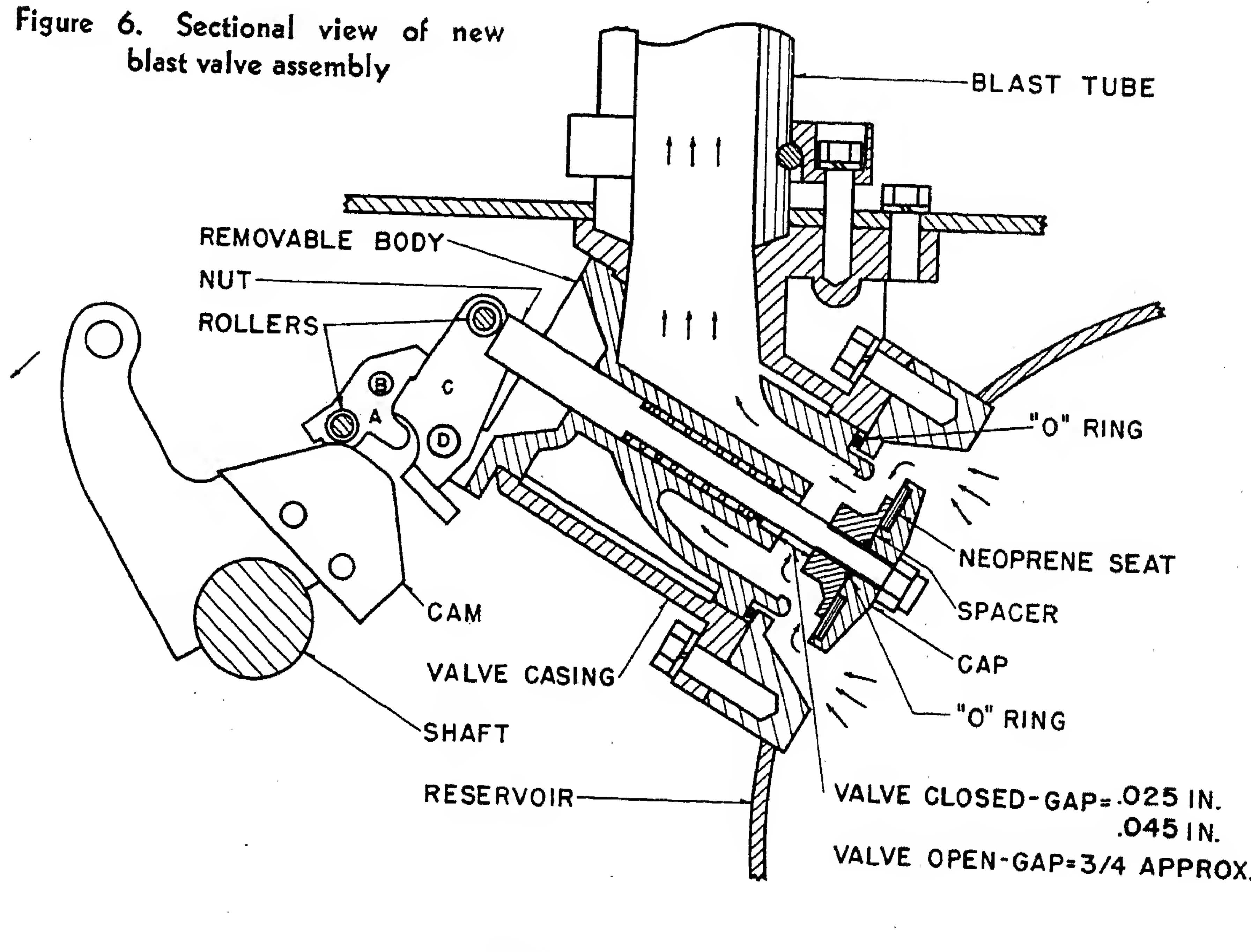
vided which, working in conjunction with a new mechanism, permit greater closing effort with less mechanical slam and rebound on the contact members. Rebound of circuit-breaker contacts on opening has been reduced to less than 10 per cent. The mechanism itself has been reduced 50 per cent in weight while providing greater closing effort and more exact control of air flow and forces.

Within this mechanism compartment an across-the-circuit-breaker wiring trough has been provided, open at both ends. Thus for adjacent circuit breakers, concealed wiring conveniently may be run along a line of circuit breakers. Air piping from circuit breaker to circuit breaker is mounted on top of the wiring trough. Threadless-type pressure fittings equipped with neoprene seals and providing joint flexibility used at the circuit breaker air inlet permit adding intercircuit-breaker piping by inserting this piping into the fitting and tightening the fitting bonnets with a wrench. On-the-job soldering is not required. All control wiring is enclosed in conduit or made behind the hinged control panel so that no wiring is exposed to accidental damage. The circuit breaker interlock shown to the right of the mechanism in Figure 1 has been designed to provide positive resistance to air operation when in the locked position. Even though electric contacts open to isolate the closing circuit, the interlock will hold against inadvertent manual operation of the air-closing valve. Openings shown in the left-hand door of Figure 1 provide visual inspection of the reservoir pressure gauge, circuit breaker operation counter, and position indicator without opening the mechanism compartment doors.

The requirements of a large diameter valve capable of delivering necessary quantities of air in a short-time interval and opening and closing quickly to reduce air consumption have plagued the designer with problems in metallurgy and mechanical forces which had not been solved in previous air-control experience. The blast valves have been improved to provide almost leakproof performance.

Throughout the circuit-breaker design every consideration has been given to maintenance problems. Although overall space requirements have been reduced, working clearances around the pole units with removable interphase barriers have been maintained. By reducing space requirements for the mechanism and swinging the reservoir backward, the mechanism is even more accessible than on the older design. This has not resulted in an inaccessible reservoir where insurance inspection is required, since the control panel on the left-hand side of the mechanism compartment now hinges outward and larger, more easily removable; inspection openings, acces-

Figure 6. Sectional view of new blast valve assembly



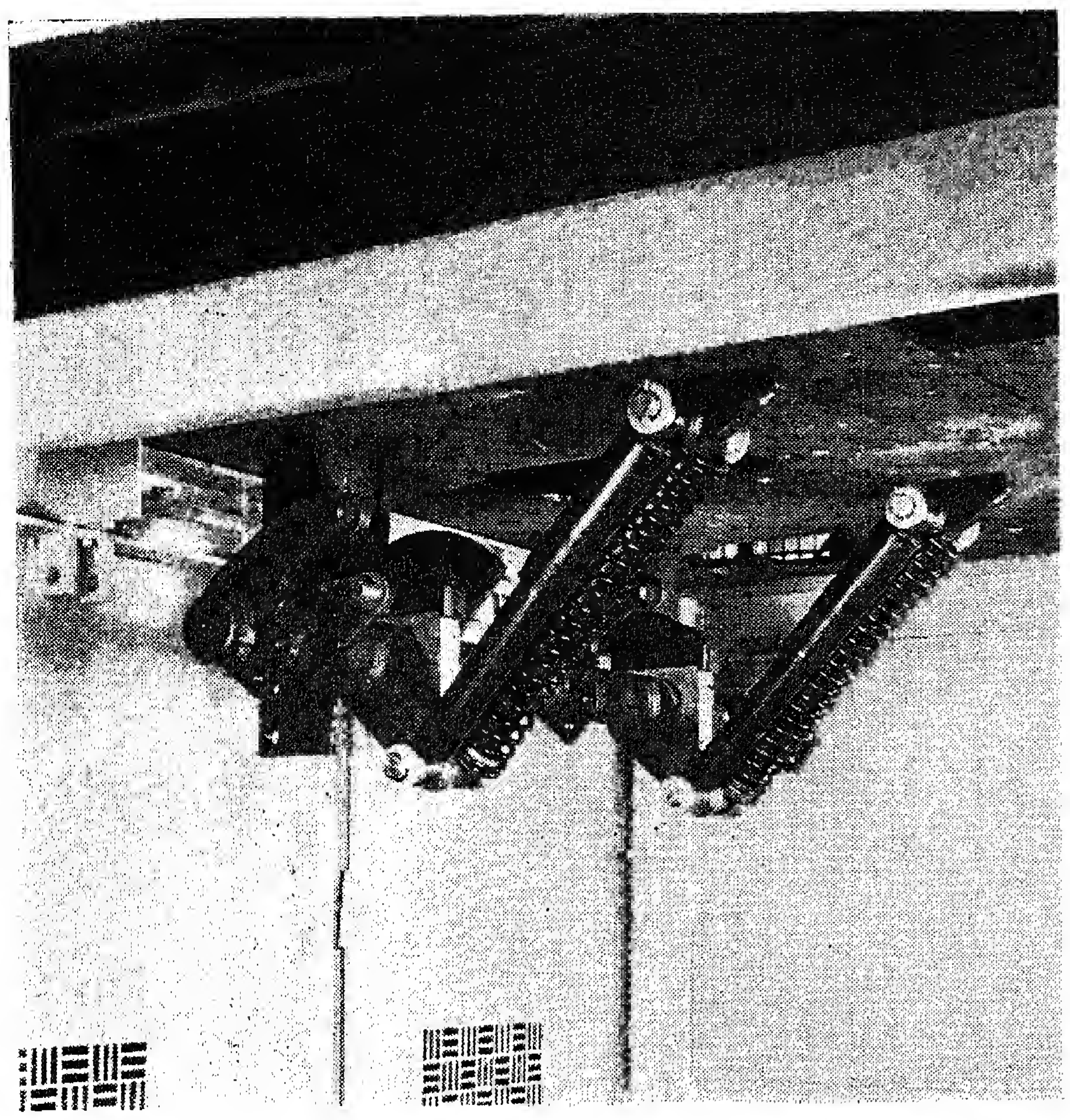


Figure 7. Shock absorber assembly showing mounting of tubular-type shock absorbers

moving contacts to dimension providing correct deflection and then silver plated. The block also is slotted perpendicularly to the entrance slot to provide a multiplicity of contacts. Alignment is obtained by engagement with the moving contact and then tightening the holding bolts, thus positioning the finger block. Further adjustment is unnecessary. Heavy railroad-type lock washers are used, capable of maintaining the finger block position and contact. Located directly above the finger block, Figure 4, and above the center finger block in Figure 5 is an elkonite arcing tip. Thus as the contacts separate, the arc is moved away from the finger block to the arcing tip by action of the air blast and magnetic forces, protecting the finger block from a stationary arc. The moving contacts are made from cupaloy castings.

To insure homogeneous castings free from flaws and porosity, new X-ray techniques have been adapted. Many apparently sound castings from a visual inspection were rejected when X-rayed. The economics of this problem would not permit the resulting scrap rate. This forced the development of new casting procedures which today have largely eliminated rejects and insure industry a uniform product for this application.

Conduction of current across the hinge joint in combination with mechanical movable members has been troublesome

sible from the front, have been provided.

In the older design, where service requirements were not fully appreciated, adjustments were provided. Besides the difficulty in maintaining these adjustments a greater amount of know-how was required by maintenance personnel. In the modern circuit breaker most adjustments have been eliminated. Contact pressures are fixed by machined dimensions and spring design such that bolting tightly into place automatically results in correct adjustment. Extensive tooling eliminated adjustments in link lengths, stop settings, and so forth. Multiple finger contacts have eliminated the need for close fitting of contact surfaces.

Arc Chute

A modern 1,500,000-kva arc chute with one side removed is shown in Figure 3. While the component parts have remained essentially unchanged, the geometry of the chute has been altered to give more efficient air usage. The arc chute proper has been lengthened, but at the same time bulky mufflers previously used have been eliminated. This results in a narrower arc chute and a reduction in handling weight of 37 per cent. It is notable that recent development of a laminated fiber by fiber manufacturers has reduced warpage of arc-chute material roughly 60 per cent. Spare arc-chute splitters and parts retain their shape and facilitate replacement, even after months on the storeroom shelf.

Contacts and Pole Units

Figures 4 and 5 show 2,000-ampere and 4,000-ampere pole units respectively, both for a 1,500,000-kva circuit breaker. The stationary contacts consist of cast cupaloy finger members bolted to the main stationary terminal casting. Cupaloy having a conductivity of 83 per cent, heat treatable to a Brinell hardness of 100, and having a yield strength three and one-half times that of cast copper, possesses the current-carrying capacity and spring tension required for this application. Thus the need for adjustable springs and fastening gadgets has been eliminated. The cupaloy block is slotted for entrance of the

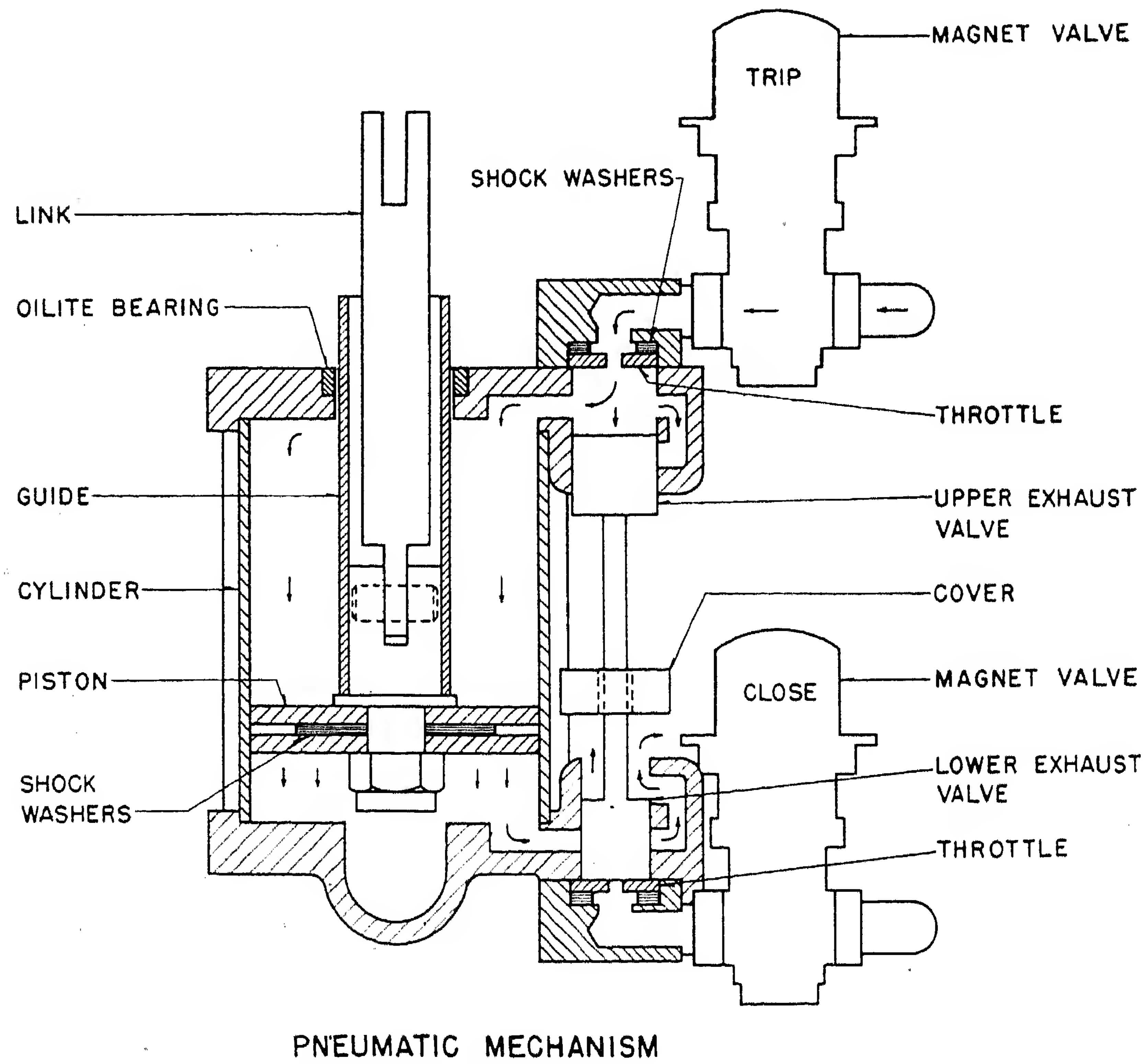
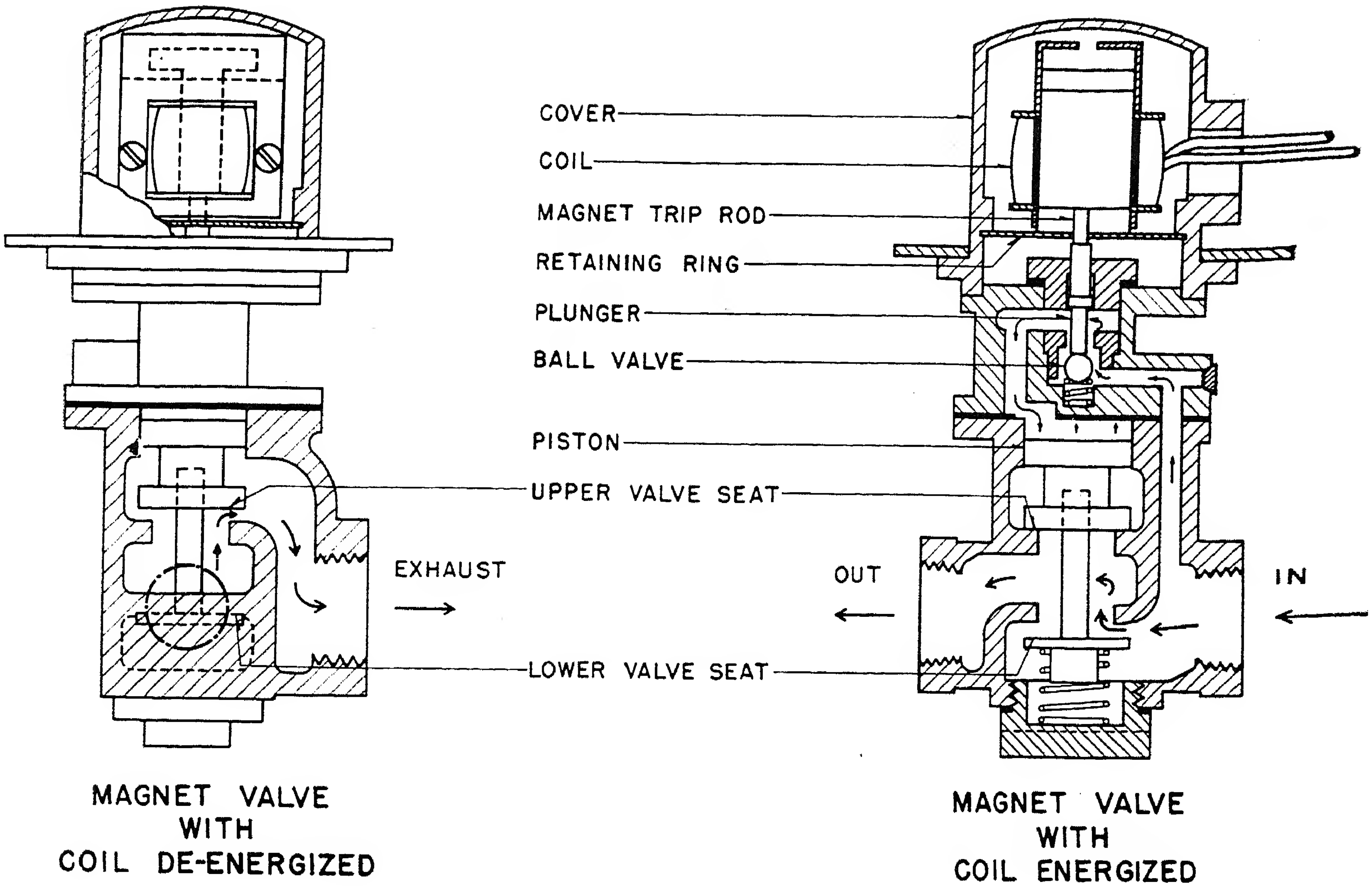


Figure 8. Pneumatic mechanism assembly in section

Figure 9. Sectional view of magnet valve



in the past. Various combinations of sliding plates and flexible shunts have been used with some success. The solution shown here has given excellent results. It embodies a multiplicity of contacts bridging the stationary and movable members shown separated from the circuit breaker in Figure 4 and assembled in Figure 5. Each contact is maintained with individual biasing springs over both moving and stationary members. Correct contact pressure is maintained without adjustment and no adjustment can be made affecting the mechanical working of the joint. Since line contact only is maintained between the bridging and movable members and since each line contact is spring biased individually, any tendency of the silver-to-silver surfaces to gall is overcome by the bridging member lifting and riding over the surface discontinuity rather than working a ball of silver or foreign particles back and forth to damage the contact. Wiping action on the stationary of the hinge is obtained by permitting a small back and forth drag as the moving contact rotates. To increase the current-carrying capacity of the hinge joint, more bridge members are used. The assembled contact bridges, contained in their respective cover, are easily removed for examination.

Blast Valves

The new blast valve is shown, in section, in Figure 6. Previous designs utilized metal-to-metal seats which although successful in their operation required considerable skill in seating. A correctly seated valve had long life and leaked less as it was operated. However, if the valve seat and piston did not register over nearly the whole circumference, point stresses were set up due to the force of closing, the metal was deformed beyond its elastic limit, and permanent damage resulted requiring excessive regrinding to reseat. The new design utilizes a neoprene seat that is restrained in its deflection by a metal stop other than the valve seat. In addition, the valve seat itself is made wide to reduce seat pressures. This results in a dependable seating, long life valve. No regrinding or precision field work is necessary. A valve seat accidentally damaged, may be corrected by installing a new valve seat disc. As indicated in Figure 6, the valve assembly may be removed as a unit without disturbing other supporting members.

Shock Absorbers

Figure 7 shows the new application of tubular-type shock absorbers to control

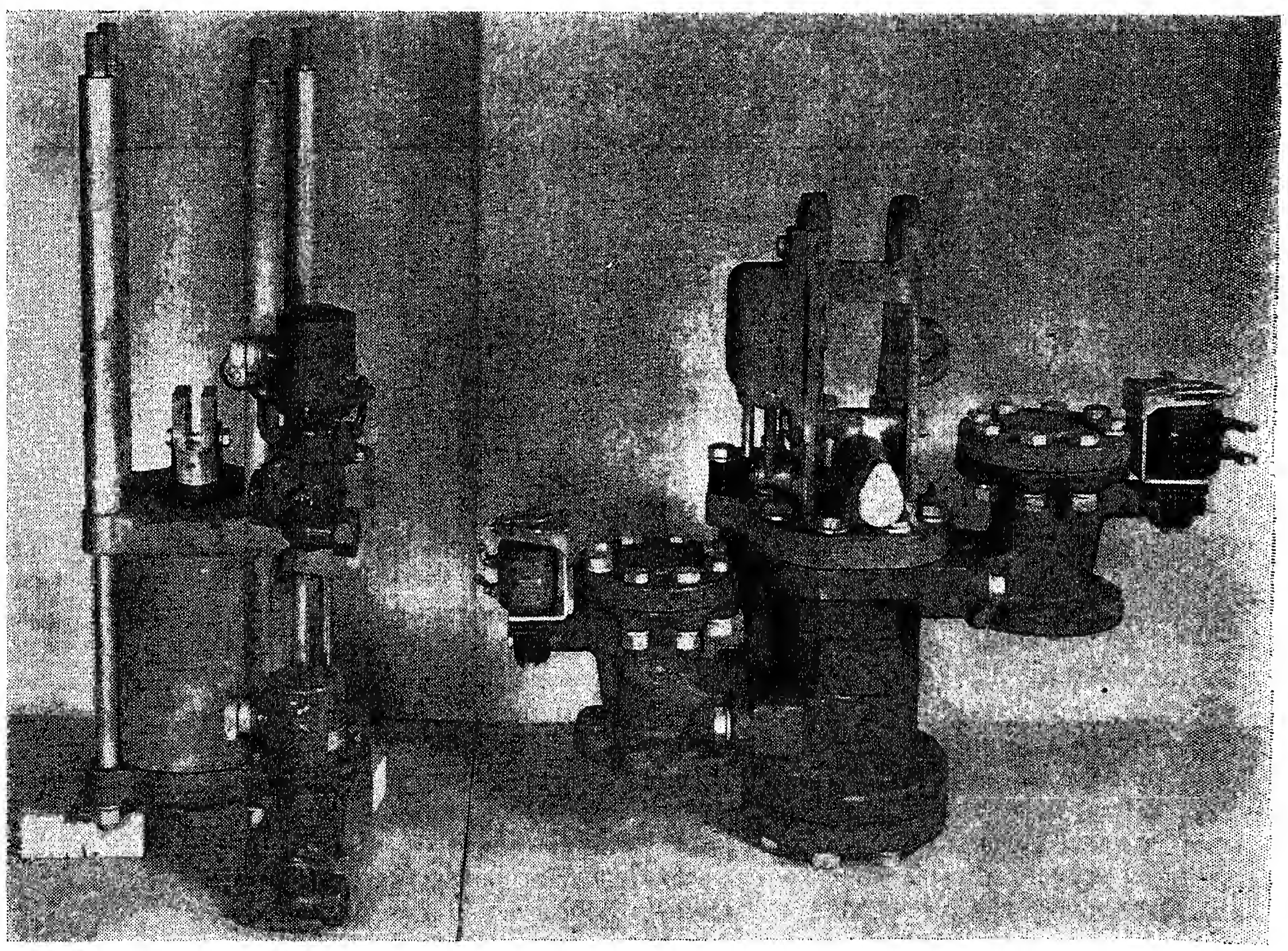


Figure 10. Old and new designs of pneumatic mechanisms

opening and closing motion at the end points. This linkage works over both sides of the toggle position having more overtoggle on opening than on closing. Previous attempts to control rebound and impact by air dash pots and compression of air within the main mechanism cylinder were not successful due to compressibility of the air and the use of the same air pockets for unrelated functions. Usually an undesirable compromise was necessary between stopping effort and the opening or closing function. The use of shock absorbers divorced these functions, making finer control of each possible.

Working the shock absorber on the closing stroke presents no problem because the new mechanism does not exhaust the driving air unless immediate contact reversal is necessary. Thus, if the circuit breaker slows due to magnetic forces, the shock absorber action also slows, reducing resistance, and the mechanism supplies the force necessary to pass through the slow point.

Mechanism

The operating mechanism has been modernized as shown in Figure 8. This mechanism consists of the main cylinder and piston, the closing and opening magnet valves shown in Figure 9, and a selective functional-type exhaust valve assembly mounted between the magnet valves and the main cylinder.

This mechanism is referred to as functional in that when the closing magnet valve is energized to admit air below the main piston, air is maintained until the circuit breaker is fully closed and the magnet valve de-energized by an auxiliary switch unless contact reversal is required due to short-circuit conditions. Then the opening valve is energized by relay action to introduce air above the main piston. This air acting on the upper exhaust valve piston forces the lower exhaust valve piston downward (since the lower piston has smaller area) cutting off the closing air and exhausting the air beneath the main piston to atmosphere. This contrasts with the older type mechanisms where exhaust of the driving air was obtained by mechanically linking the exhaust valve piston to the shaft and exhausting when the piston reached a certain position. This was referred to as a positional exhaust. Obviously a shock absorber used in closing in combination with exhausting of the driving air would stall the circuit breaker. The older circuit breakers were required to absorb more shock on contacts and connecting linkage, and required finer adjustments to insure proper closing.

An interesting comparison of the old and new type mechanisms, side by side, is shown in Figure 10. The older mechanism, on the right, was designed with generously large cast cylinders and flanges requiring bulk and space. By employing a rolled tubular cylinder held together with the studs and closed at the ends with simple castings, the new main cylinder has resulted. The older mechanism shows a magnet valve mounted on a relay air valve assembly which is then mounted to the main cylinder casting. This auxiliary equipment has been replaced with a compact unit assembly, shown also in Figure 9, light in weight, easily removable for bench inspection, and easily stocked as a spare part. Whereas the older mechanism weighed 250 pounds, the new mechanism weighs 125 pounds.

Other Applications

This paper has indicated use of the new design features only on the 1,500,000-kva circuit breaker. Their use on the 1,000,000-kva circuit breaker at 1,200- and 3,000-ampere continuous current capacity closely paralleled that of the 1,500,000-kva circuit breakers. To complete this line of circuit breakers, 2,500,000-kva circuit breakers are now being produced for 14.4-kv service and 34.5-kv circuit breakers with an interrupting capacity up to 2,500,000 kva and a continuous current rating to 3,000 amperes, all designed to incorporate the new features.

Discussion

A. M. deBellis (Consolidated Edison Company of New York, Inc., New York, N.Y.): This paper describes the first major revision of design in the mechanism and parts of the Westinghouse 14.4-kv air-blast circuit breaker line since they were first furnished to us in 1942. Actually, the main principles of design are not changed in this revamping of the circuit breaker, which speaks very well for the men who built the first of this type of power circuit breaker.

We are now operating approximately 115 units of the original design and these, by and large, have been satisfactory. A number of improvements are described in the paper by Mr. Schrameck, which I feel will improve the ease of maintaining the circuit breakers. Also we should experience fewer maintenance requirements as a result of the simplifications adopted in the mechanism.

Some leakage has been experienced at the hard-blast valve seats of the old design, which should be eliminated by the new Neoprene seat material. Special care will have to be taken, not only in the design of the Neoprene seat but also in its assembly in the valve, to prevent displacement of the seat material during the circuit breaker operation, which would result in more serious air leakage than has been experienced in the past with metal-to-metal seats.

Robert H. Nau (University of Illinois, Urbana, Ill.): The most popular reasons for cussing or discussing a person or thing are interwoven with the proclaiment's desire to tell either of his close association with or to extol achievements of the recipient.

The basis of this discussion is supported by both of these factors. I must signify, however, that I intend to mention my engineering association only to exemplify my qualifications to give plausible comments rather than perfunctory remarks upon this new compressed-air circuit breaker.

During the first 4 years of the development and utility application period of this high-power compressed-air circuit breaker, I was pressing the development of the Westinghouse magnetic De-ion air circuit breaker.[1,2] Inasmuch as the lower interrupting ratings of the cross-blast compressed-air circuit breaker overlap the higher ratings of the magnetic circuit breaker, my thinking and interests with both circuit breakers were poignant. Since the early years, 14 of my discussions on circuit breakers have been published in AIEE *Transactions*. In the last 2 years I have written two major articles on modern-type high-power circuit breakers.[3,4]

The prototype of the new 14.4-kv indoor compressed-air circuit breaker was eminently successful in interrupting the highest short-circuit currents from the time of its inception. Whether the manufacturer was able to realize any profit or not at that time is problematical.

I made a detailed study of the new circuit breaker which Schrameck aptly describes in his paper. The improvements in mechanical design are of such striking quality that the circuit breaker is, indeed, new, except for functional operation and superficial appearance. The 30-per-cent reduction in weight signifies tremendous advantages to both the customer and the manufacturer. The development of precision castings for the operating mechanism and contact parts represents significant scientific advances.

Shop and maintenance difficulties have been practically eliminated by new design features incorporated into the various parts, that is the arc chamber, the current carrying parts, and the operating mechanism. The machining operations for the arc chamber parts used to be tedious and costly in order to obtain the necessary space for side dumping of the blast air during the high current part of the arcing period.[5] A very simple machining operation on the fiber splitters is now used to facilitate similar effects.

The main stationary contacts are now made of single solid blocks of cast cupaloy into which one large groove has been machined and several slots have been cut to introduce a multiplicity of self-spring biased tuning fork contacts. These contacts require no elaborate adjustment and can be

inspected easily. The former main contact, although entirely satisfactory, was made of many parts, including bolts, springs, and flexible shunts. A heavy rectangular piece of fiber is placed beneath the stationary contact to shield conveniently the lower space from the blast air during the arcing period when high side pressures may exist.

The contact bridge that connects the moving wishbone contact arm with the lower stationary stud is particularly novel. This bridge eliminates both troublesome shunts and abrasive side-pressure contacts.

The innovation of employing neoprene valve seats has enhanced the operational life of the valves, removed the nuisance of compressed-air mechanisms, and made valve servicing in the field a minor consideration. These Neoprene valve seats give practically leak-proof performance.

In addition, it is significant to note that the interrupting performance of the new 14.4-kv indoor compressed-air circuit breaker surpasses that of its prototype.

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J. E. Schrameck: The writer wishes to thank Mr. deBellis and Professor Nau for the emphasis each has given the features of the subject compressed-air circuit breaker such as contact structure, blast valves, simplicity, and general accessibility for maintenance purposes. Mr. deBellis' experience with 115 units of the original design is particularly interesting and certainly contributes to the general fund of information which has brought about wide acceptance of the indoor compressed-air circuit breaker.

In early experiments with neoprene-seated blast valves, displacement of the valve seat, as pointed out by Mr. deBellis, occurred. This displacement was eliminated by better support and added protection for the valve disk. In thousands of subsequent circuit-breaker operations on a test circuit breaker and on several hundred circuit breakers now in service, no seat displacement has occurred.

Pneumatic Operating Mechanisms for Power Circuit Breakers

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PNEUMATIC operation of power circuit breakers which started before 1900 was used very little until the last decade. The increasing energy required for closing circuit breakers of higher and higher rating and the demand for quicker closing and reclosing enhanced the advantages of the pneumatic mechanism and led to its present predominance in the operation of large power circuit breakers. Experience gained with the mechanisms which pioneered the applications and gained widespread use has influenced the development of a new line of devices which are used on power circuit breakers over the voltage range of 230 kv to 14.4 kv, interrupting capacity range from the highest down to 500 megavolt-amperes, and interrupting times of 8, 5, and 3 cycles.

These mechanisms are mechanically trip-free in all positions to make it impossible to hold the circuit breakers closed by compressed air when the trip coil is energized. Ciruit breakers operated by them are spring-opened and, whether tripped electrically or manually, their opening performance is entirely independent of the air pressure existing in the compressed-air system.

The various power circuit breakers impose widely different loads on the mechanisms during a closing stroke. Most of the circuit breakers have relatively light closing force requirements during the early part of the closing stroke while the moving contacts are being accelerated and lifted into engagement with the stationary contacts, and relatively heavy force requirements during the latter part of the stroke while the interrupters are being closed and energy is being stored to provide for the opening operation. Lever systems provide mechanical advantages which smooth out these requirements to a large degree.

Before pneumatic operating mechanisms were widely used, the circuit-breaker loads were fitted to the solenoid mechanisms, and many of these circuit breakers are still in use. Occasionally, such circuit breakers are changed over to pneumatic operation. Moreover, many types of circuit breakers, particularly these requiring light or moderate closing forces, are built for operation in some locations by pneumatic mechanisms and in others by solenoid mechanisms, and their load characteristics should be suitable for either type of mechanism. Consequently, it is desirable to be able to get closing force characteristics from a pneumatic mechanism which are suitable for these circuit breakers.

The pneumatic mechanism has much more force than a corresponding solenoid at the start of a closing stroke. Although this is an advantage in increasing the initial acceleration and reducing the closing or reclosing time, this force, if sustained, may result in excessive contact speed before the heavier loads near the end of the stroke are picked up. These mechanisms have adjustable throttle valves which provide for limiting the contact speed by restricting the supply of air during the early part of the stroke and then increasing the supply of air during the latter part of the stroke.

The operation of the wide range of power circuit breakers is accomplished by two basic sizes of pneumatic mechanisms. The smaller size, Figure 1, is designed for operation at a maximum initial air pressure of 170 pounds per square inch and has a piston diameter of 7 inches and a piston stroke of $5^1/_2$ inches. By varying the operating pressure or the throttling of the device, this mechanism can be fitted to the range of circuit breakers requiring closing efforts of approximately 22,000 inch-pounds and less.

The second and larger pneumatic operating mechanism, Figure 2, is designed for circuit breakers generally of

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The development program which produced the operating mechanism described in this paper involved the efforts of many individuals, each contributing his knowledge, skill, and advice. The authors wish to mention in particular J. B. MacNeill, who co-ordinated the development and contributed his extensive experience on switchgear requirements.

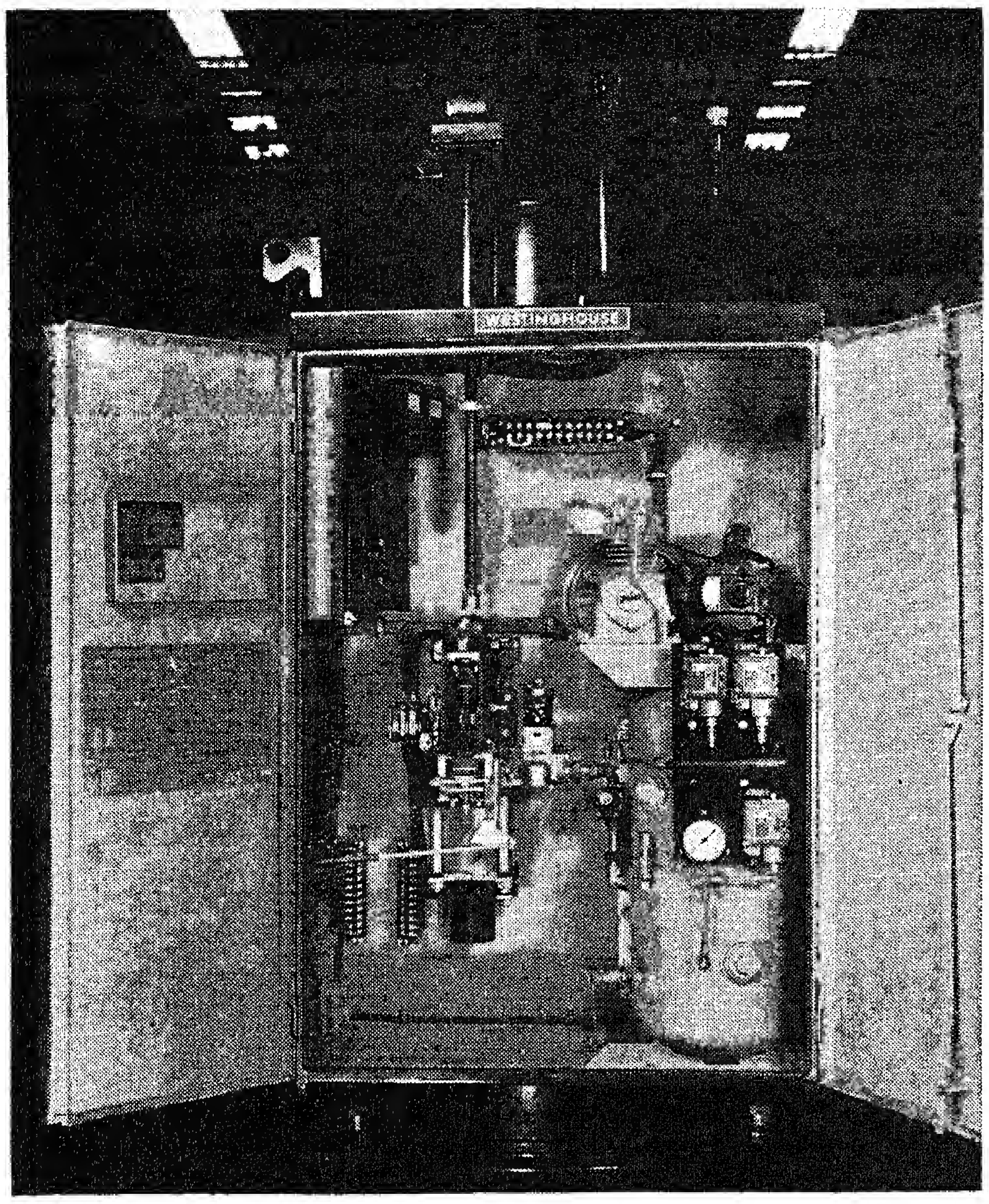

Figure 1 (left). The smaller of two types of mechanically trip-free pneumatic operating mechanisms for power circuit breakers

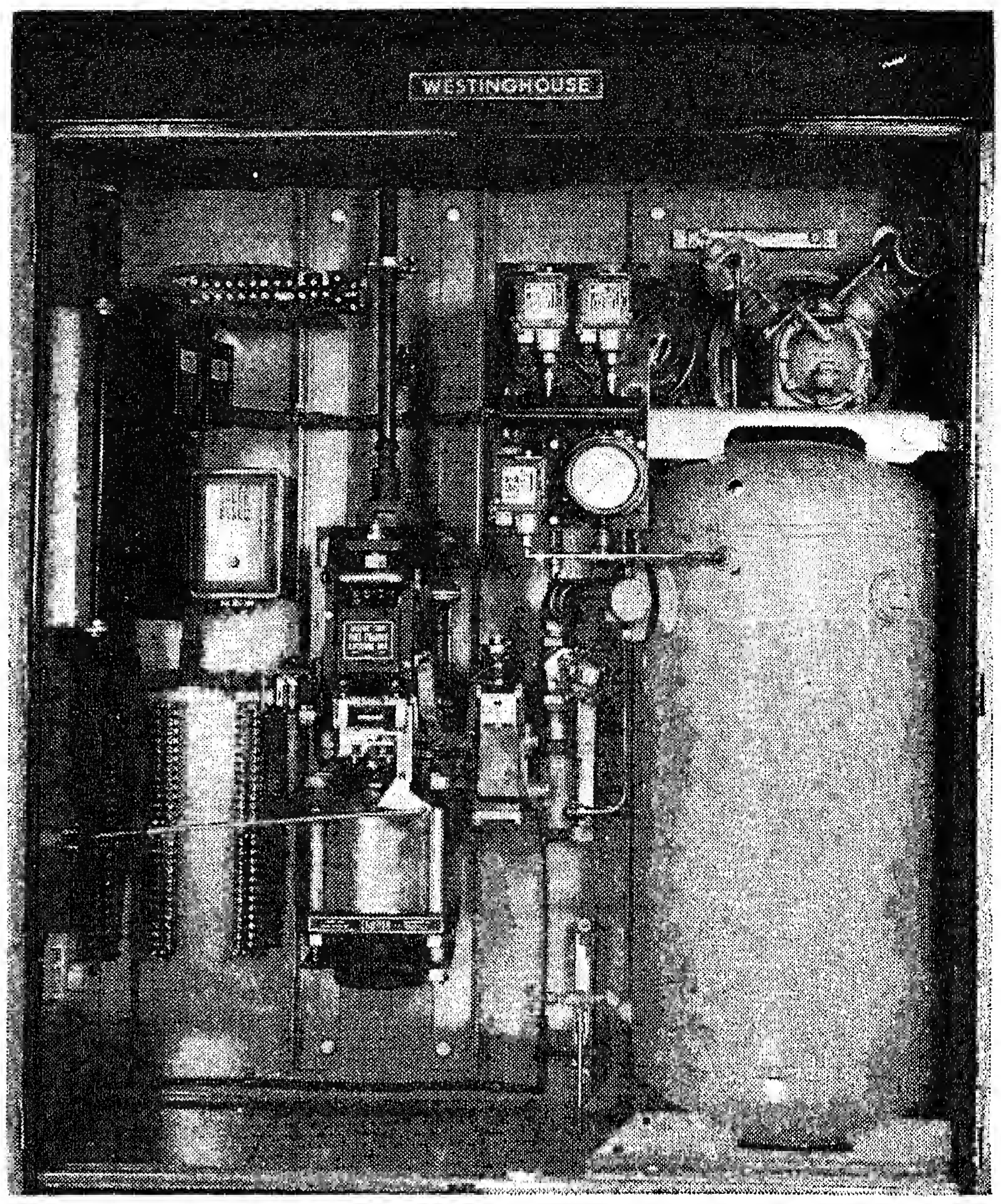

Figure 2 (right). The larger of two types of pneumatic operating mechanisms for high-speed reclosure of high-voltage power circuit breakers

3,500 megavolt-amperes and above. It has a cylinder diameter of 10 inches and a piston stroke of $5^1/_2$ inches. When this mechanism is used for loads up to about 45,000 inch-pounds and at an operating pressure below 180 pounds per square inch, a single-stage compressor suffices. It can operate circuit breakers requiring up to about 70,000 inch-pounds if supplied with air at pressures up to 270 pounds per square inch by a 2-stage compressor and larger reservoir. This larger mechanism usually is supplied with a throttle when used on circuit breakers having an interrupting capacity of 5,000 megavolt-amperes or less, but with circuit breakers rated above 5,000 megavolt-amperes the mechanism usually is applied without a throttle, and adjustment in the output is obtained by varying the reservoir air pressure.

Description

The smaller of these mechanisms is shown in cross section by Figure 3. At the upper left-hand side of the figure the mechanism is shown in the open position, below that in the closed position, and at the upper right in a trip-free position with the piston held by air at the lower end of its stroke.

The driving member consists of a nonferrous piston moving in a nonferrous cylinder clamped between two steel cylinder heads. Air, admitted through a port (not shown) in the upper cylinder head, drives the piston to the lower position. The force on the piston is transmitted through a vertical piston rod to a cross head and pin *B* equipped with rollers operating between guide rails forming part of the fabricated frame of the mechanism. Operating within these same guide rails is another roller-equipped pin *A* which transmits the force to the circuit breaker pull rod. These two pins are connected together by two links, labeled closing link and cam link, which are restrained at their common point, pin *C*, by an intermediate link attached to the latching system. As long as the pin *D* in the right-hand end of this intermediate link is held stationary by the latches, the piston and the circuit breaker pull rod are mechanically connected together and the force on the piston is transmitted almost unmodified to the circuit breaker pull rod. During the closing stroke, therefore, the circuit breaker pull rod and the piston are mechanically connected and move together until they reach the closed position shown in the lower part of Figure 3. Here the holding latch biased by a spring engages the cross head and holds the lower pin and the piston in the latched position.

During this closing operation, pin *D*, which connects the right-hand end of the intermediate link and the trip-free lever, is held in a fixed position by the trip-free trigger which engages a roller on the trip-free lever. If at any time during the closing stroke, or while the circuit breaker is in the closed position, the trip-free trigger releases the roller by the action of the trip rod, the trip-free lever becomes free to rotate in a counter-clockwise direction, and the right-hand end of the intermediate link can move to the right, thereby releasing the two links which join the piston rod to the circuit breaker pull rod. Releasing the circuit-breaker pull rod permits the circuit breaker to move to the open position under the action of accelerating springs.

If compressed air is acting on the closing piston at the time the trigger operates, the piston will move to, or be held in, the closed position and the levers may reach the position shown in the upper right section of Figure 3. However, the opening of the trip-free latches and the opening of the toggle will cause the cam link to prevent the latch from holding the cross head. It accomplishes this by pushing the holding latch out of engagement with the cross head if it has engaged it in the end position, or by preventing it from engaging if the tripping occurs before the end position is reached. As soon as the compressed air is released from the cylinder, the piston will return to the open position under the action of the retrieving springs mounted below the piston. While the closing piston is returning to the open position, the closing link and the cam link again will come into toggle relationship. and the intermediate link will pull the trip-free lever into its latched position so that the trigger can engage the roller and the mechanism can become ready for another closing operation.

This resetting operation can take place at any point of the opening stroke. If the tripping occurs while the mechanism is in the closed position with no air pressure in the cylinder, the retrieving springs will

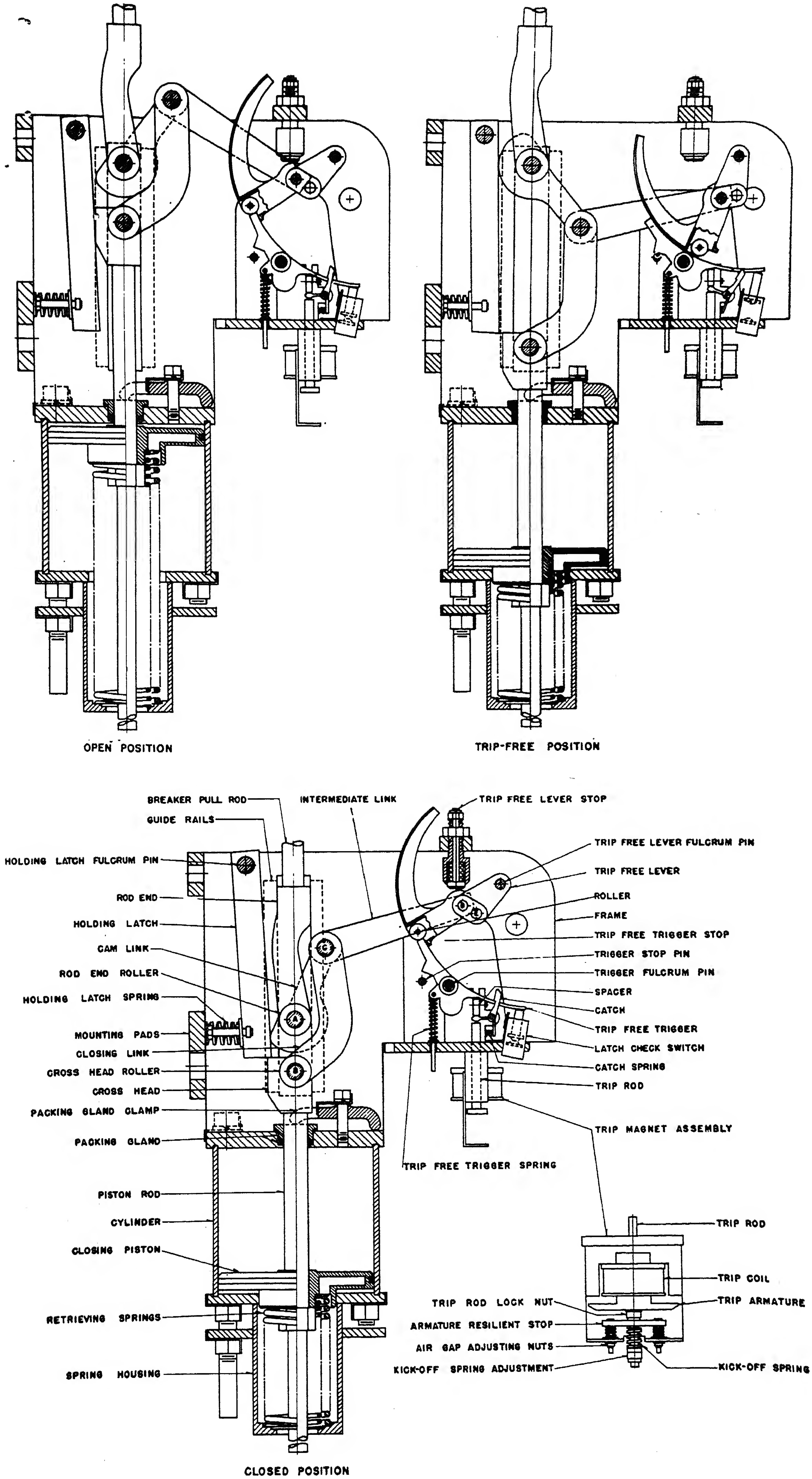

Figure 3. Cross section of the smaller pneumatic operating mechanism in the closed, a trip-free, and the open positions

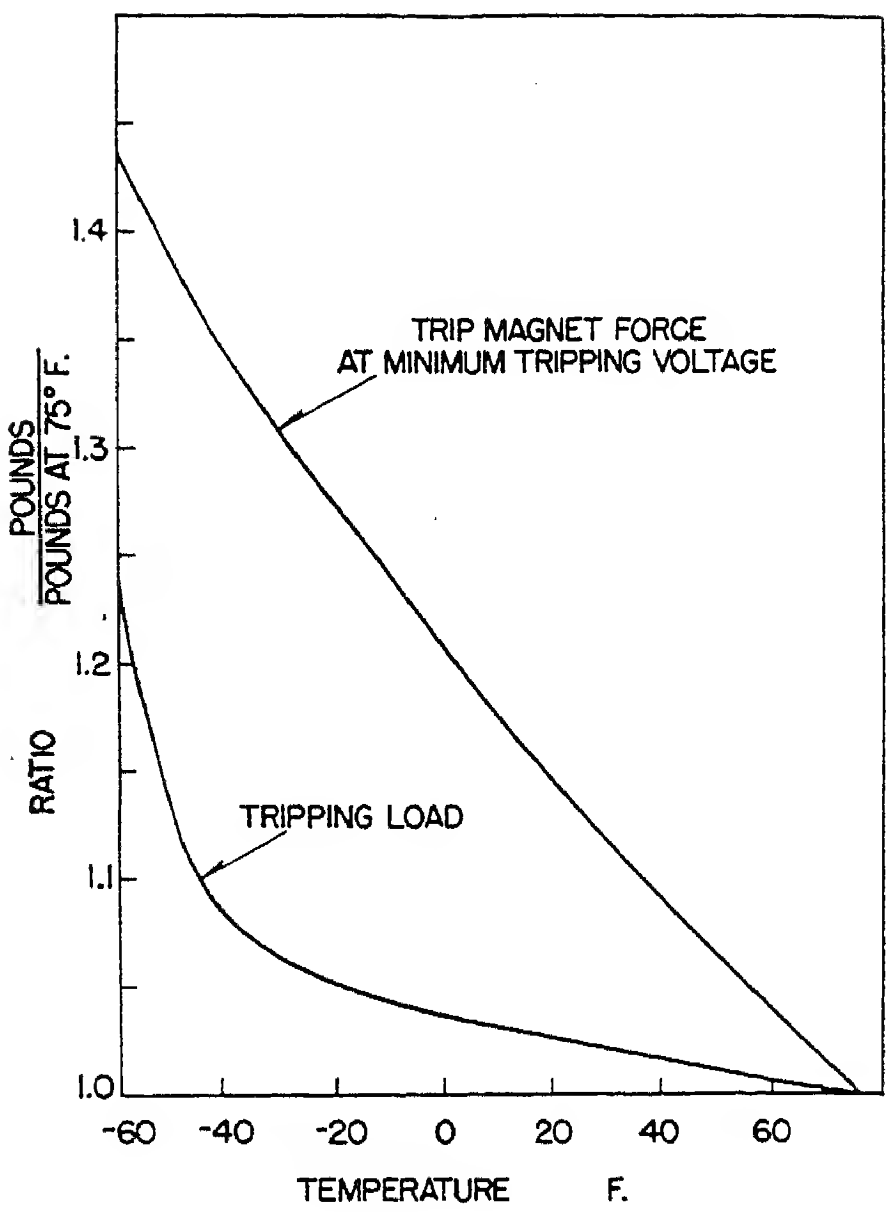

Figure 4. Low temperature increases the force exerted by the trip more than the load of the latches

accelerate the closing piston as soon as the cam link releases the holding latch which occurs within a small fraction of an inch of pull rod travel. The retrieving action is rapid, and before the open position is reached the piston may attain the position relative to the pull rod which permits the trigger to engage. When this occurs, the closing piston again is positively coupled to the circuit breaker pull rod, and air pressure can be applied for the reclosing of the circuit breaker.

To prevent accidental release of the trip-free trigger due to shock, a small catch engages the trip-free trigger and is released by the trip rod before the latter engages the trip-free trigger.

The trip-free trigger and the roller on the trip-free lever are mounted on roller bearings. These bearings are lubricated with a grease suitable for operation within a range of temperatures corresponding to those encountered in circuit-breaker operation. As a check that cold weather would not cause these bearings to become inoperative, a latch and trigger mounted in a mechanism frame and loaded to a service value were put in a refrigerator and reduced in temperature to −60 degrees Fahrenheit. At this point the force required for tripping had increased about 23 per cent. As temperatures are lowered, the available force exerted by the trip coil because of lowered resistance increases relatively more than the tripping load as shown by Figure 4. This indicates that the margin to insure positive tripping is not as great at high temperatures as at low. However, even at 150 degrees Fahrenheit and minimum specified tripping voltage, the trip magnet can exert about 30 pounds more than the force required to release the trigger.

The trip magnet is a completely self-contained assembly, Figure 5, easily removed from the mechanism as a unit by withdrawing just two bolts. Although it is designed for high-speed operation, the iron circuit is not laminated. This results in a strong lightweight armature. Tests showed that the increase in the current necessary to compensate for the eddy currents in the solid cores was very small and reduced only slightly the rate of increase of the tripping force.

The ampere-turns at minimum control voltage are chosen to produce a flux density approaching saturation because a magnet having a given amount of flux produces a greater force if the flux is concentrated in a smaller area. The force necessarily increases as the control voltage is raised but does not become excessive even at maximum control voltage. Operating time was reduced by locating the center air gap inside the coil to reduce leakage and increase the flux densities in the air gaps.

A spring-mounted bar supports the armature in the open position and prevents vertical shocks from driving the armature upward and causing accidental opening of the circuit breaker. Its location is adjustable and controls the normal air gap of the trip magnet. The trip rod attached to the armature and transmitting the force to the trigger is adjustable so that the trigger can be released at a suitable point prior to the end of the armature travel. A small spring, compressed near the end of the tripping stroke, accelerates the armature away from the stationary core as soon as the trip magnet is de-energized. This facilitates rapid resetting of the trigger. A conventional hand-tripping knob on a rod projecting through the mechanism housing acts on a small bell crank at the lower end of the trip rod to force it upward and release the trigger.

Compressed air for the operating cylinder is admitted and discharged through a port in the top cylinder head. Mounted immediately above it is a control valve combining the functions of an inlet valve and a pressure release or exhaust valve, Figure 6. An electromagnetic pilot valve admits control air to act on pistons above the exhaust valve and above the inlet valve. This control air performs two functions: first, it seals the exhaust valve, and second, it opens the inlet valve to admit air to the operating cylinder. The use of only a small volume of control air produces rapid valve action. The air is conducted to the cylinder through a divided passage. One part goes through a small variable orifice, or by-pass opening,

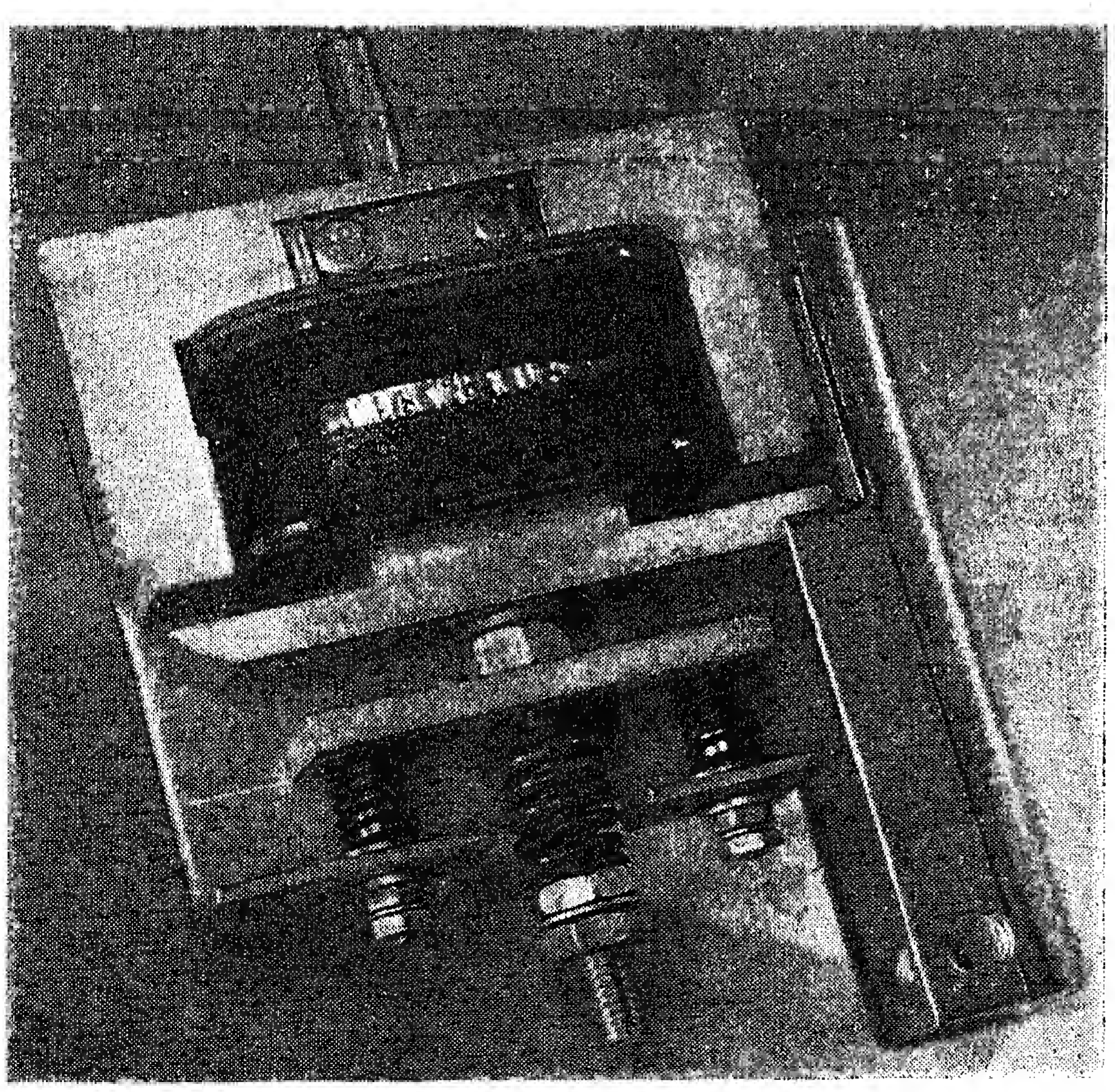

Figure 5 (left). The trip magnet used on both mechanisms is a sturdy, easily adjusted subassembly

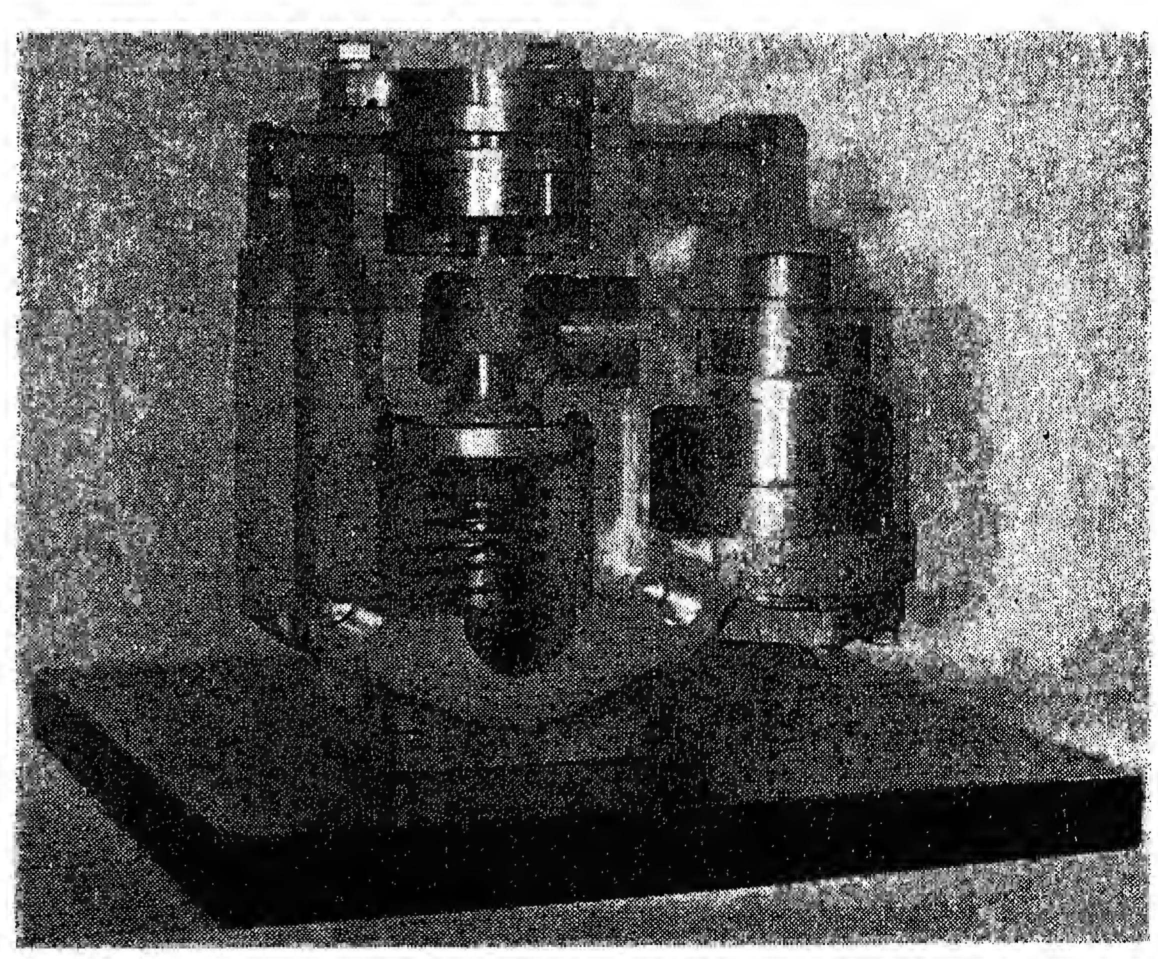

Figure 6 (right). A control valve for the larger mechanism cut away to show the control air passages and cylinders, and the inlet and exhaust valves

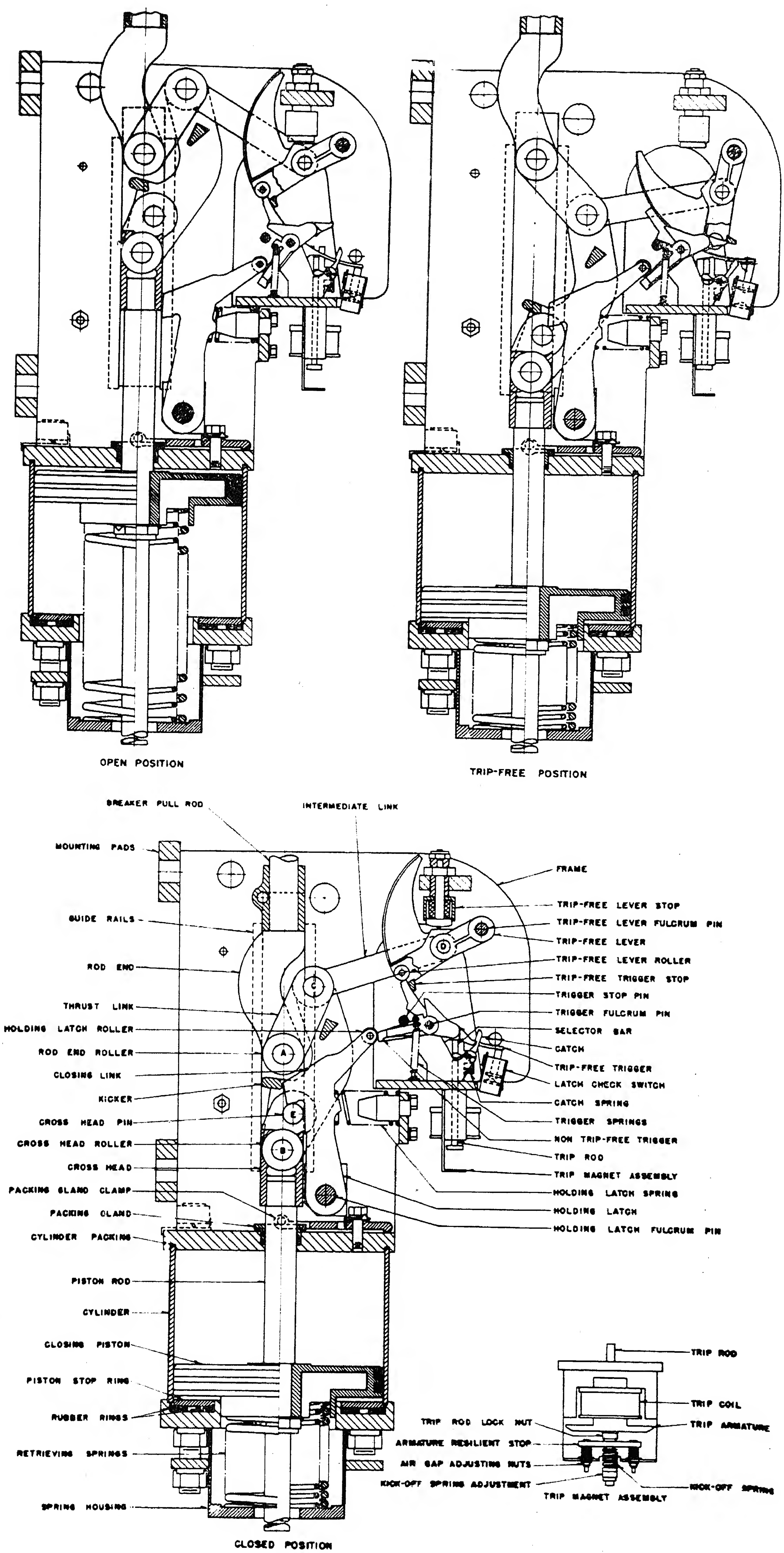

Figure 7. Cross section of the larger of the trip-free pneumatic operating mechanisms for power circuit breakers. This mechanism opens nontrip-free except when the operating cylinder contains compressed air

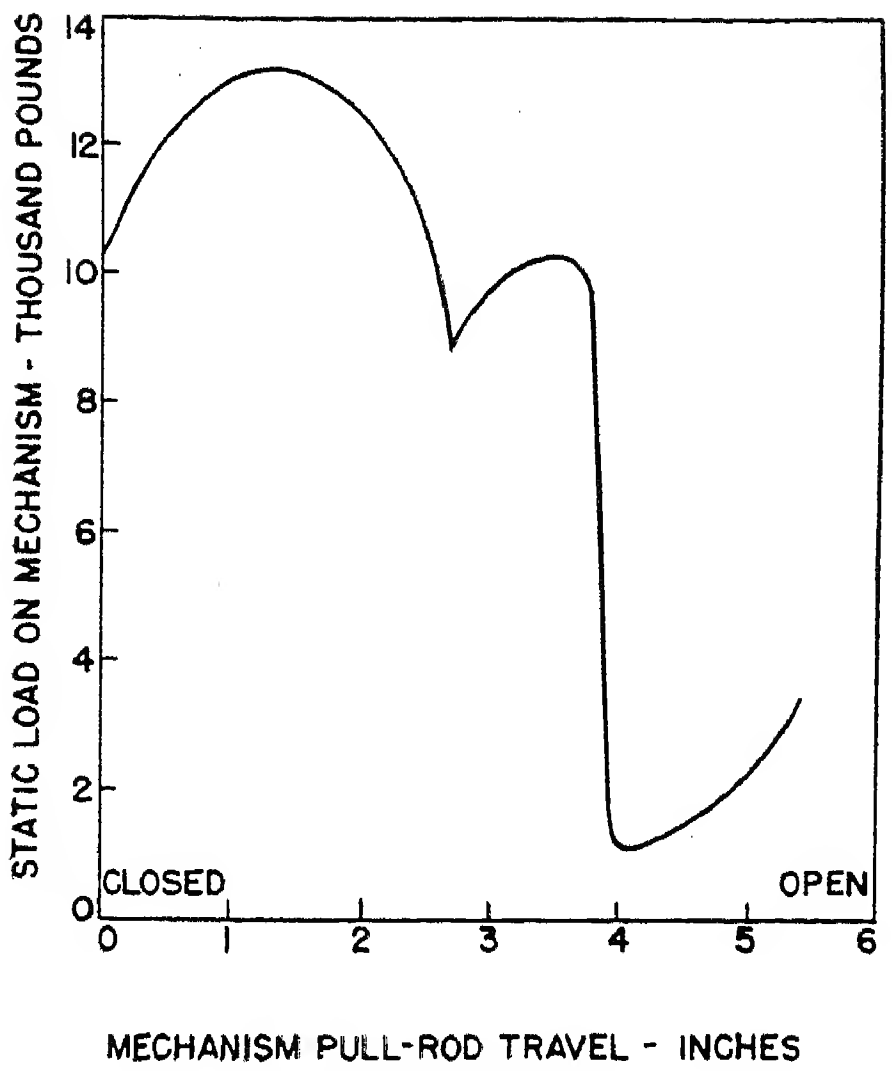

Figure 8. The load imposed on the larger mechanism by a 161-kv 10,000-megavolt-ampere oil circuit breaker. The additional energy supplied by the mechanism in the early part of the closing stroke becomes the kinetic energy of the moving parts

Table I. Performance Data

	Mechanism Smaller	Mechanism Larger
	Time in Cycles, 60-Cycle Base	
Closing		
Time to complete opening of pilot valve	1.1	1.1
Time to start movement of lift rod	1.9	2.5
Time to close circuit breaker (touch contacts)	11.0	12.1
Times measured from energizing pilot valve coil		
Opening		
Time to release trigger	0.6	0.6*
Time to start movement of lift rod	1.0	1.0
Time to part circuit-breaker contacts	2.0	1.6
Time for circuit breaker to reach open position	8.4	6.6
Times measured from energizing trip coil		
Reclosing		
Time to release trigger	0.6	0.6*
Time to energize pilot valve coil	5.7†	1.7
Time to complete opening of pilot valve	7.1	2.8
Time to reverse circuit-breaker lift rod motion	8.3	8.0
Time to reclose circuit-breaker contacts	16.7	14.6
Percentage opening of circuit-breaker lift rod at reversal	90	73
Times measured from energizing trip coil		
2nd Immediate Reclosure		
Time for circuit breaker to reach open position	9.0	7.2
Time to reset trip-free linkage	14.5	11.7
Time to complete opening of pilot valve	15.7	12.9
Time to reclose circuit-breaker contacts	26.1	24.5
Percentage opening of circuit-breaker lift rod at reversal	94	100
3rd Immediate Reclosure		
Time for circuit breaker to reach open position	8.7	7.2
Time to reset trip-free linkage	14.0	12.0
Time to complete opening of pilot valve	15.1	13.1
Time to reclose circuit-breaker contacts	26.0	25.3
Percentage opening at circuit-breaker lift rod at reversal	100	100
Times measured from energizing trip coil		

* These data apply to both nontrip-free and trip-free opening.
Time to reset trip-free linkage.

which is controlled by a by-pass adjusting screw. This passage supplies all of the air during the early part of the closing stroke and can be adjusted to reduce the air pressure in the cylinder. The second and larger passage contains a throttle valve which remains closed during the early part of the stroke. It is opened mechanically to increase the air supply to the cylinder at a predetermined position by a roller on the pin through the rod end. At the end of the closing stroke the pilot valve is de-energized. This discharges to the atmosphere the air in the control cylinders, thereby closing the inlet valve and practically simultaneously opening the large exhaust valve which quickly vents the air from the operating cylinder.

A considerable portion of the energy produced by fast-acting operating mechanisms goes into the kinetic energy of the moving parts of the circuit breaker and mechanism. This kinetic energy must be dissipated at the end of the closing stroke and might result in a considerable shock to the apparatus. Part of the kinetic energy in this operating mechanism is absorbed by an air cushion at the end of the closing stroke. In the last 3/4 inch of travel a cylindrical projection on the lower surface of the operating piston seals a hole in the lower cylinder head and traps a small quantity of air below the piston. This air is compressed and, its pressure, rising rapidly immediately before the piston reaches the end of the travel, greatly reduces the shock on the operating mechanism. When the piston is moving in the other direction, a check valve prevents the formation of a partial vacuum in this chamber which would reduce the acceleration of the piston and delay the resetting of the trip-free latches.

Manual operating devices provide maintenance operation without the use of compressed air. When the mechanism is used on frame-mounted circuit breakers where the closing effort needed is relatively low, the circuit breaker can be closed with one sweep of the handle of a

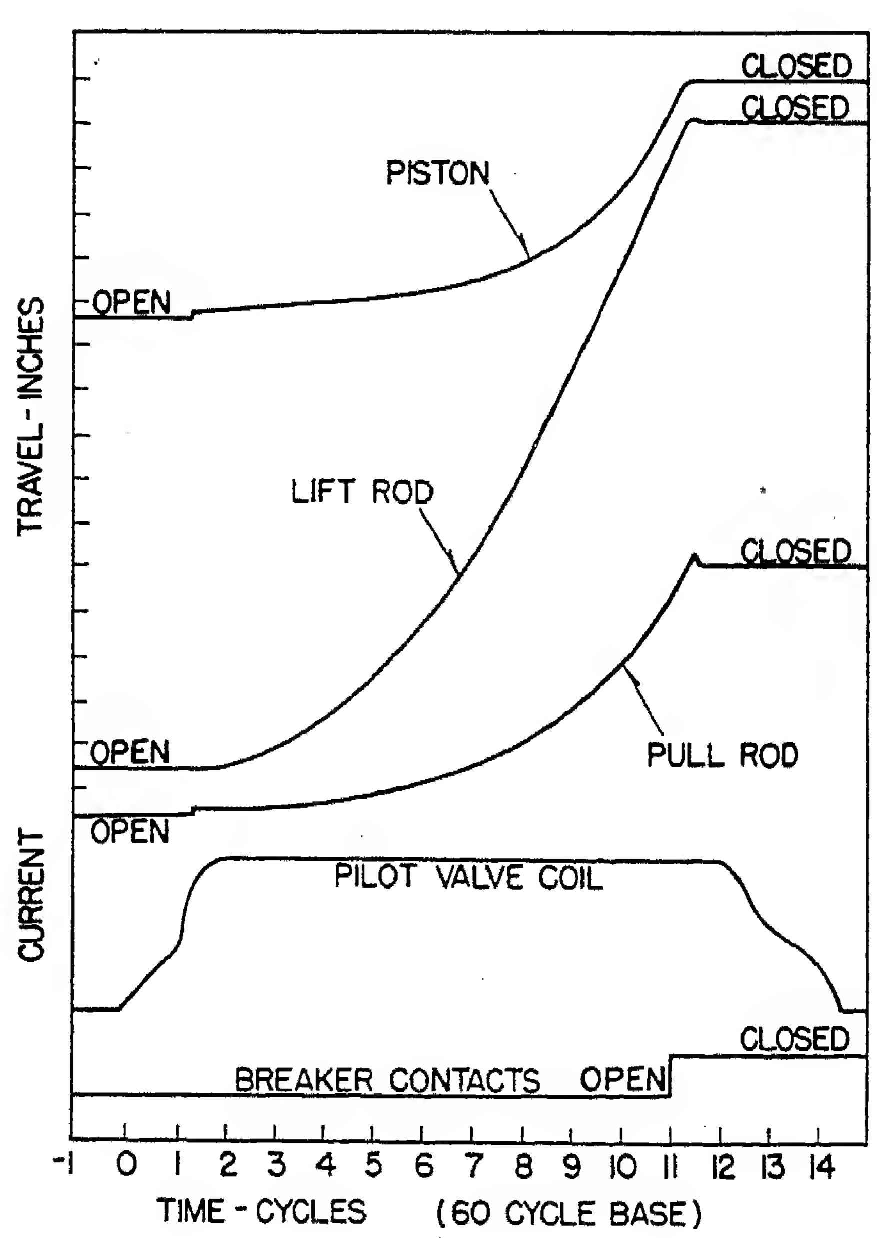

Figure 9. The smaller operating mechanism closing a 115-kv circuit breaker in 11.0 cycles

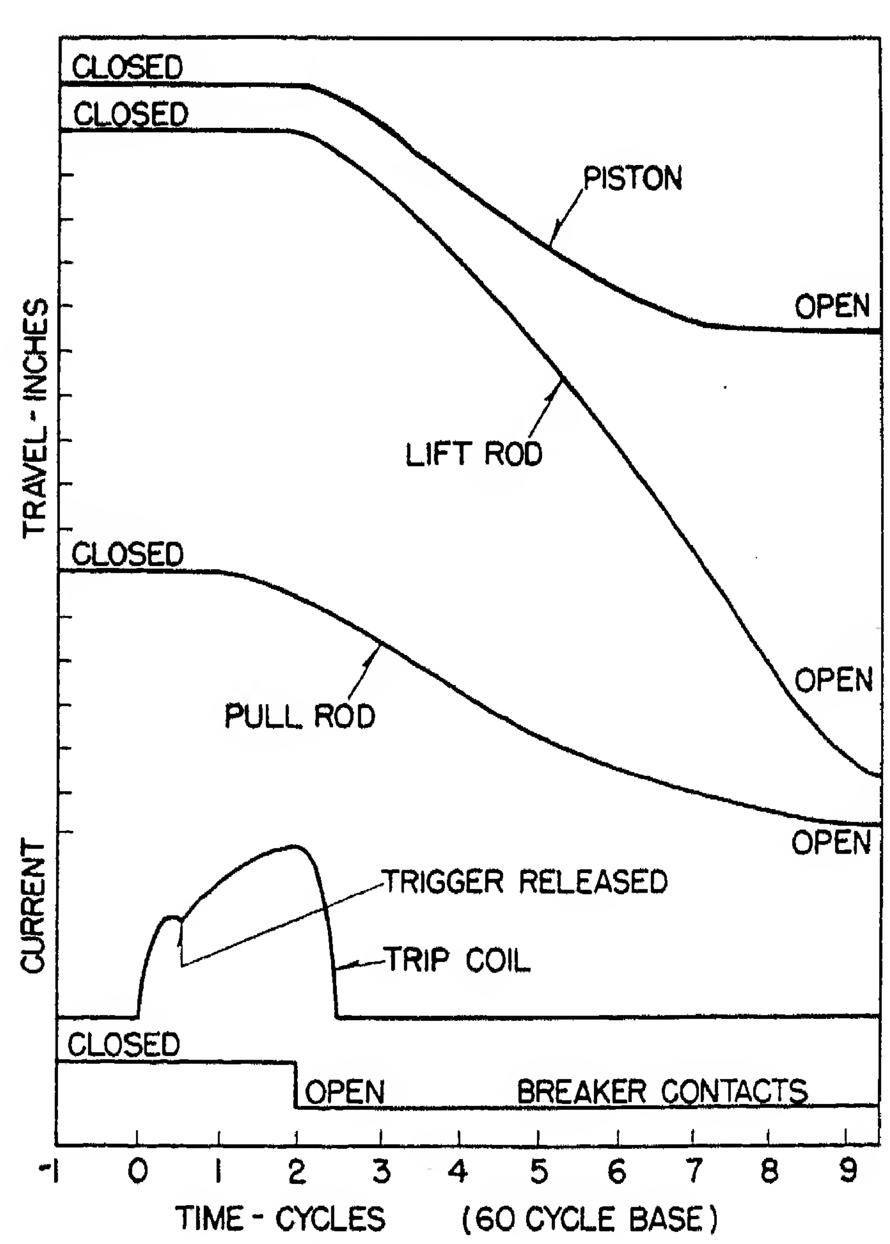

Figure 10. The smaller operating mechanism opening a 115-kv 5-cycle circuit breaker

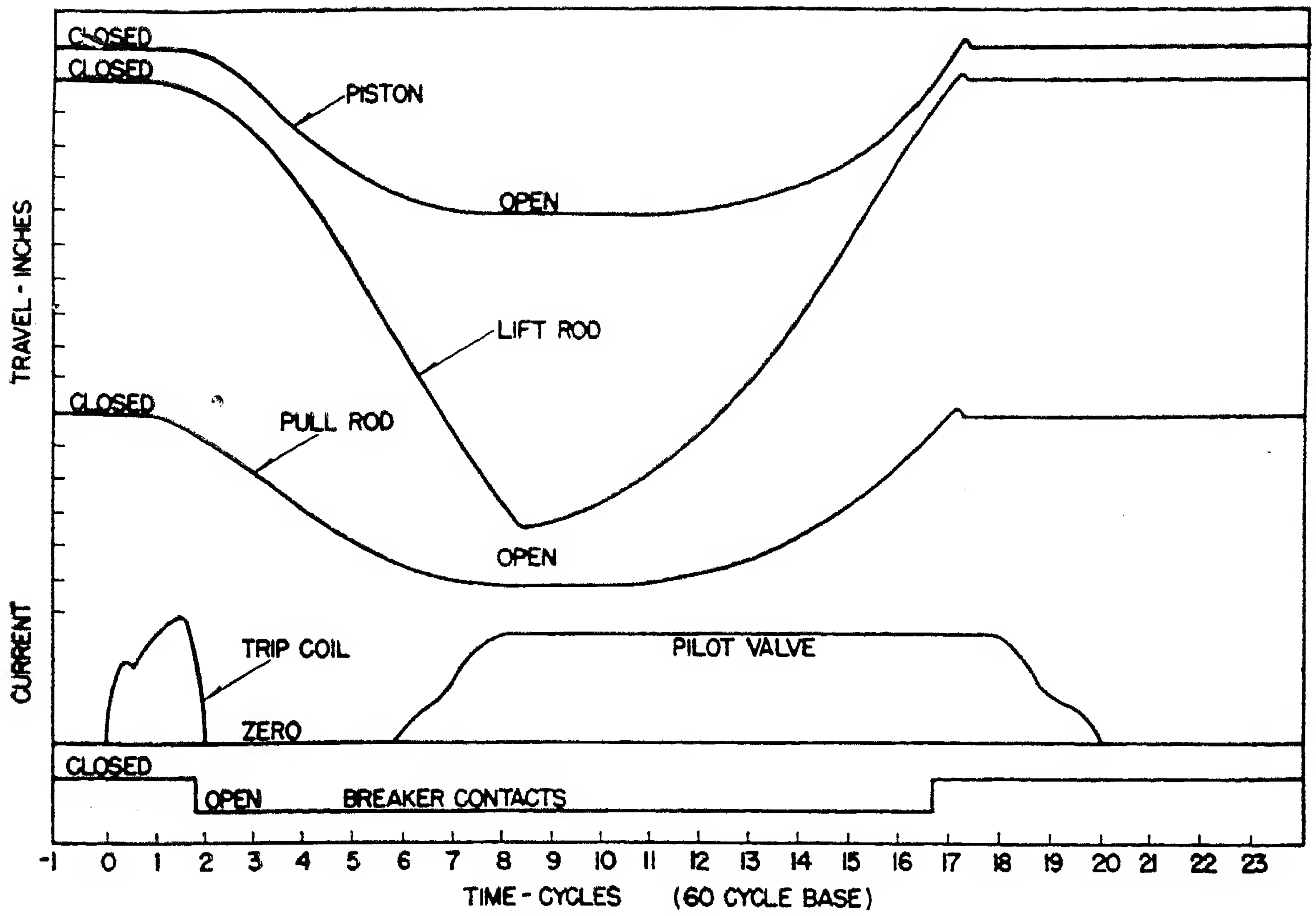

Figure 11. The smaller operating mechanism reclosing a 115-kv circuit breaker in 16.8 cycles

slightly above the minimum satisfactory operating pressure, a low-pressure cutoff switch operates to open the closing circuit, thus preventing the mechanism from attempting to operate the circuit breaker when there is insufficient air pressure to complete the operation. The reservoir tank fulfills the requirements of state inspection codes, and all equipment is manufactured under the American Society of Mechanical Engineers requirements with close inspection.

In addition to the mechanism and control valves which have been discussed in detail, there are other auxiliaries essential to the functioning of the mechanism, such as a control panel for mounting the $X-Y$ relays and knife switches, auxiliary switches, pressure switches, safety valve, compressor unit, heaters, pressure gauge, operation counter, and terminal blocks. The mechanism and these auxiliaries are mounted inside a sheet metal housing, Figure 1. The door openings are sealed around the edges with tubular neoprene rubber gaskets to make the housing weatherproof and dustproof. In the placement and arrangement of the equipment in the cabinet, the emphasis was on accessibility. The single-stage air-cooled compressor and its driving motor are mounted on a bed-plate located on top of the reservoir. The junction box for the motor leads is out in front, the drain and filling plugs for the compressor crank case are readily accessible at the left-hand side of the compressor, and either the motor or compressor can be removed easily as a unit without disturbing other apparatus.

club-type hand-closing device which is easily removable for transfer from one mechanism to another. When the mechanism is used where the closing effort exceeds that which a man can exert in one stroke, the circuit breakers are closed by a ratchet-handle screw-type jack operating on a threaded section of the piston rod.

Accidental trip-free operation of the circuit breaker during maintenance can be prevented by inserting a pin through holes in the side plates of the frame. This pin passes behind the catch and just above the tail section of the trip-free trigger, thereby preventing the trip-free trigger from releasing the trip-free lever roller.

If necessary, the operating cylinder of the mechanism can be inspected thoroughly without disturbing the levers and latches above it. This is accomplished by removing first the spring housing and retrieving springs below the piston, and then the lower cylinder head and cylinder wall. This exposes the piston which can be removed from the lower end of the piston rod.

Rust is a problem in devices which operate infrequently. Bearing surfaces are especially important as the successful operation of the devices depends on their ability to move after long inactive periods. In this mechanism the cylinder and piston are nonferrous. The piston rod is chrome-plated and runs in a nonferrous bushing in the cylinder head. The guide rollers are stainless steel in the smaller mechanism and manganese bronze in the larger mechanism. The pins, made from heat-treated alloy steel, are carried in bronze or antifriction bearings and are lubricated to prevent rusting. All parts of the control valve are made of nonferrous material with sliding surfaces of moving parts, such as pistons and valve stem, chromium-plated to prevent abrasion. Surfaces which are not part of bearings are painted, plated, or made of nonferrous materials.

The compressor is a single-stage air-cooled type. A pressure governor switch regulates the pressure in the storage reservoir. A safety valve is supplied on the reservoir to prevent pressure from building up to a dangerous level. At a pressure

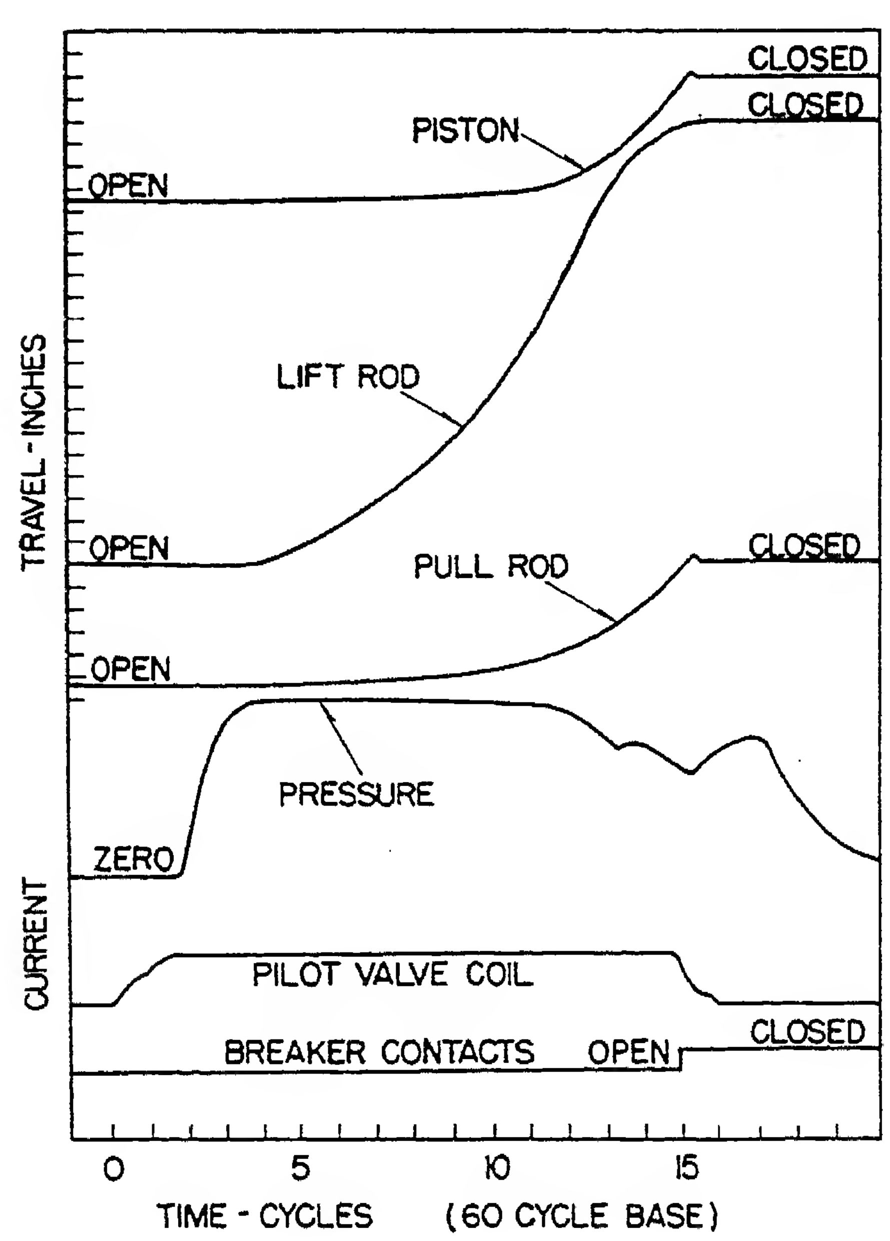

Figure 12. The larger operating mechanism closing a 161-kv circuit breaker in 15.0 cycles

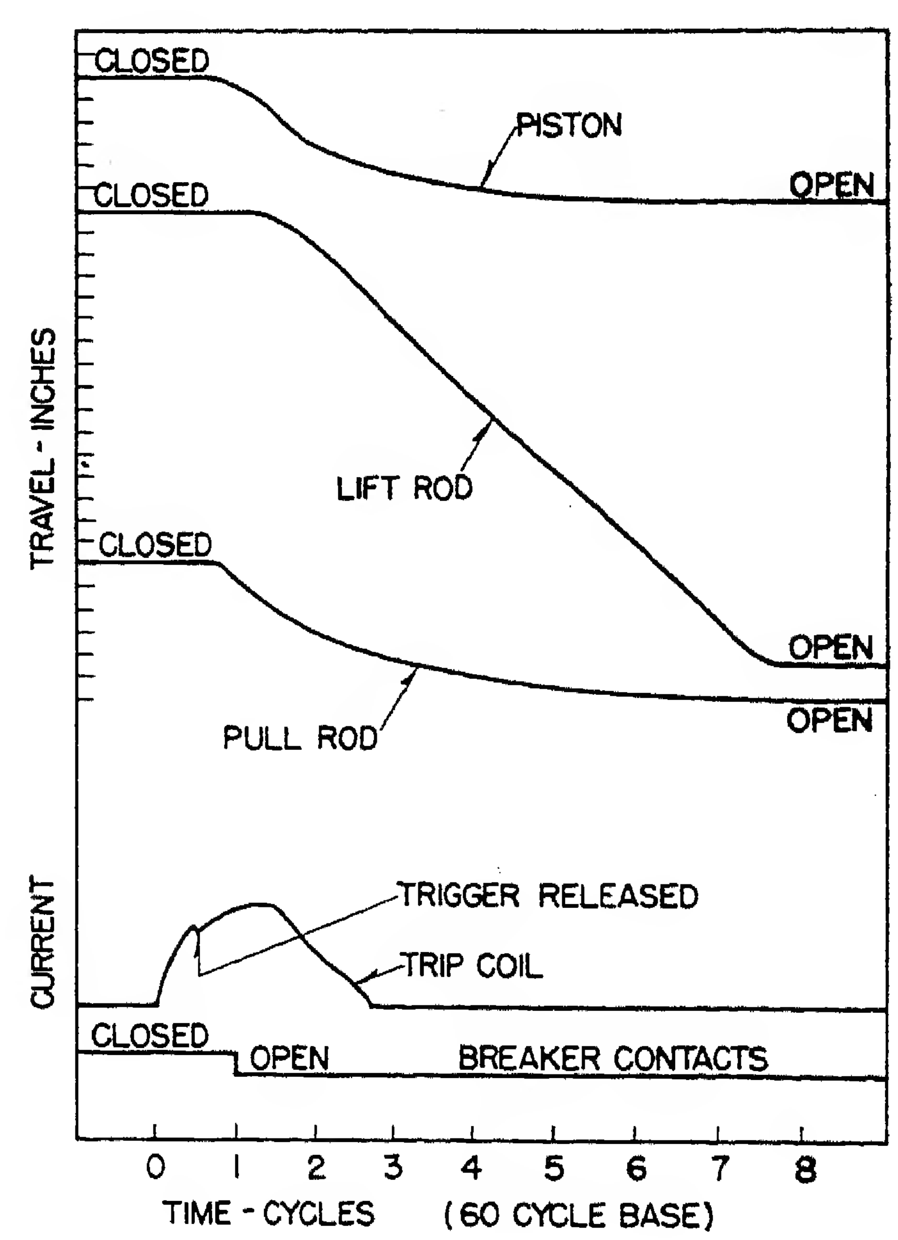

Figure 13. The larger operating mechanism opening a 161-kv 3-cycle circuit breaker

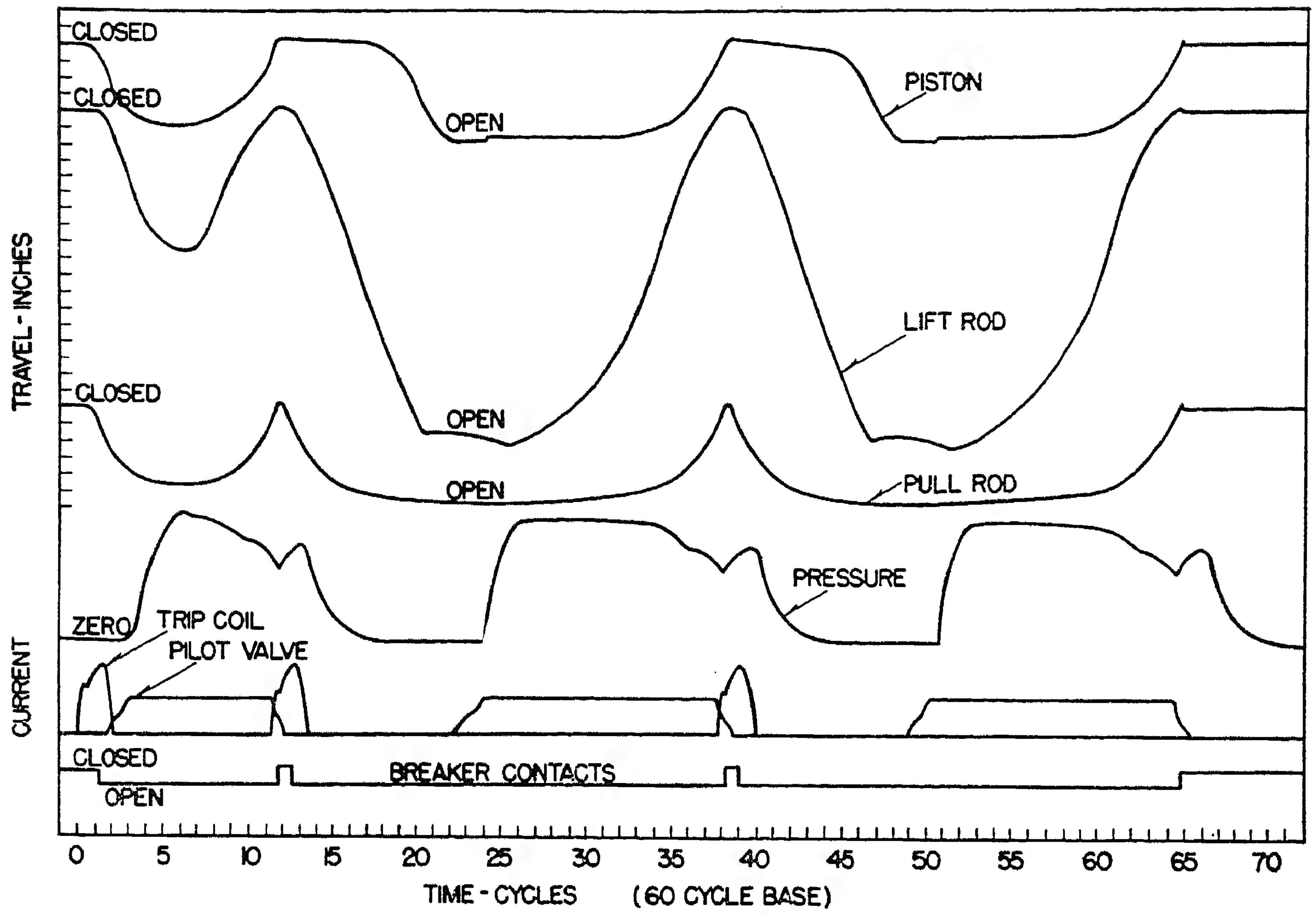

Figure 14. The larger operating mechanism reclosing a 161-kv 10,000-megavolt-ampere circuit breaker three times in 66 cycles. The first opening is nontrip-free with reclosure in 11.5 cycles, the second and third trip-free with reclosures in 26.3 and 27.0 cycles

The piping between the reservoir and the control valve is simple and direct with rubber compression-type fittings at either end to facilitate the alignment and provide the desired flexibility. The pressure switches and pressure gauge, are conveniently located on a panel on the front of the reservoir for ease of reading and inspection. By locating the latch check switch, triggers, and trip magnet at the front of the mechanism, they are readily accessible for inspection. The cutoff switch, located on the left front side of the frame, has individually adjustable finger contacts. The control panel, mounted on hinges, can be fastened back out of the way in its normal service position or swung outward for access to the wiring connections on the rear. The 11-pole auxiliary switch, which is located on the back of the housing is operated from the pull rod. The adjustment of the position of its contacts is made by simply shifting the position of a clamp on the pull rod. A syphon-type drain, which has proved very reliable on railroad car equipment for years, is employed for removing the condensate from the bottom of the reservoir. This type of drain valve prevents the accumulation of condensate above the valve seat.

The larger size pneumatic mechanism, shown in cross section in Figure 7, is similar to the smaller. It has the same type of cylinder and frame, the same type of linkage, transmitting force from the operating piston to the circuit-breaker pull rod, and the same type of trip-free linkage. Its principal differences are caused by provision for nontrip-free operation when there is no air pressure in the cylinder, a feature which is advantageous for an initial fast reclosure, because the closing means remains coupled to the circuit breaker and reclosing can be started as soon as desired. Moreover, a circuit-breaker mechanism usually is tripped while no closing force is being exerted by the closing element, for example, when the circuit breaker has been closed and is carrying current and no compressed air is acting on the piston. Under these conditions the larger mechanism functions nontrip-free since trip-free operation has no advantage over nontrip-free operation. The holding latch, which engages a pin in the piston rod cross head to hold it in the closed position, is held in the latched position by the nontrip-free trigger. Releasing the trigger releases the holding latch, thereby permitting the mechanism and circuit breaker to open under the action of the circuit-breaker load and the retrieving springs.

There may be times when the mechanism will close the circuit breaker against a short circuit and relays will energize the trip coil. Under these conditions, the larger mechanism operates trip-free in the same manner as the smaller mechanism and the circuit breaker is opened by its accelerating springs. Whenever the trip-free trigger is unlatched, a projection on it keeps the nontrip-free trigger out of its latched position so that the piston is free to retrieve when air pressure is removed. The unfolding of the toggle levers moves another projection (labeled kicker) which is part of the closing link to push and keep the holding latch out of its latching position.

Whether the trip-free or nontrip-free trigger operates when the trip coil is energized depends upon the position of a selector bar which is operated mechanically by a small piston working within the control valve. Air pressure acting on the operating piston acts also on this small piston and holds the selector bar in a position where it transmits any force from the trip rod to the trip-free trigger. As soon as air pressure approaches atmospheric in the operating cylinder, a biasing spring returns the selector bar to the position where it transmits any force from the trip rod to the nontrip-free trigger. The trip magnet can trip the circuit breaker in any intermediate position of the selector bar.

This mechanism also is particularly suited to high-speed multiple reclosing service. If while the circuit breaker is carrying load a short circuit occurs and the circuit breaker is tripped, the mechanism will open nontrip-free and a reclosing circuit can be established at any desired time. The time required to operate valves and build up the pressure in the operating cylinder allows the circuit-breaker contacts to move the few inches required for arc interruption at the same speed as during a simple opening operation. Air pressure then can reverse the motion of the lift rod before the open position is reached and return the circuit breaker to the closed position. A reversal during the opening stroke reduces the closing speed required for a given reclosing time. If the short circuit is re-established, the circuit breaker will be tripped again. However, this opening follows a closing operation so quickly that air pressure in the operating cylinder and control valve causes the trip-free trigger to be selected and the circuit breaker opens while the closing air is being shut off and the exhaust valve is opening. As soon as the force exerted by the air on the piston becomes less than the force of the retrieving springs, the piston will start toward the open position to retrieve the trip-free latches. When the trigger resets, a latch checking switch makes contact and re-establishes the closing circuit. The circuit breaker then recloses, and this second reclosure can be repeated if the short circuit is re-established again. The smaller mechanism also can be used for multiple reclosing and all openings are trip-free. The closing circuit will be made when the latches reset, and although it

can not be as early as in a nontrip-free operation, it will be much earlier than on the second and third reclosures when air is present in the cylinder at the time of tripping.

The control valve used on the larger mechanism is similar to that used on the smaller. The one shown in Figure 6 is for high-speed multiple reclosing. The detail of the small cylinder and piston for operating the selector are located on a side of the valve not shown in Figure 6.

Another model, which is applied when multiple reclosing is not required, has an exhaust valve of the spring-biased poppet type which vents the cylinder less rapidly. This exhaust valve, which is held closed by the air pressure in the cylinder during the closing stroke, opens after leakage from the cylinder has dropped the pressure enough to overbalance the spring bias.

A few of the largest high-power circuit breakers are operated with full air pressure from the very beginning of the stroke, and consequently the model of control valve for these circuit breakers has the air passage from the inlet valve to the operating cylinder entirely unobstructed by any by-pass or throttle valve.

Performance

These mechanisms already have been applied and tested on a number of types of circuit breakers. One of the circuit breakers requiring multiple high-speed reclosures has been described.[1]

In the performance data given in Table I, the smaller pneumatic mechanism was operating an 800-ampere 115-kv 5-cycle 1,500-megavolt-ampere oil circuit breaker, and the larger pneumatic mechanism was operating a 1,200-ampere 161-kv 3-cycle 10,000-megavolt-ampere oil circuit breaker having a load characteristic shown in Figure 8.

The operating performance is shown in detail in Figures 9 through 14, which were traced from oscillograms.

The new line of mechanically trip-free mechanisms as presented here is the result of several years of development planning and exhaustive testing. Their performance and endurance testing demonstrated that they should give good service. Applications have been made covering the full range of circuit-breaker sizes. The first commercial circuit breaker operated by one of these mechanisms was shipped in September 1951 and from now on most of the outdoor oil power circuit breakers built by us will be supplied with these new mechanisms.

Reference

1. High-Speed Multiple Reclosing Oil Circuit Breaker for 161 Kv, 10,000,000 Kva, B. P. Baker, G. B. Cushing. *AIEE Transactions*, volume 71, part III, 1952 (*Proceedings 52-10.*)

Discussion

H. L. Peek (Allis-Chalmers Manufacturing Company, Boston, Mass.): In the introductory paragraphs of this paper the authors particularly identify their pneumatic operating mechanisms as embodiments of the principle of mechanically trip-free operation.

Then we are told that the smaller of the two operating mechanisms is designed for mechanically trip-free operation only, whereas the larger is designed for nontrip-free operation except where opening immediately follows closing of the circuit breaker against a short circuit.

There are two particular grounds upon which the authors justify their departure from the principle of mechanically trip-free operation in the case of the larger operating mechanism: First, that the nontrip-free operation which it provides when there is no air pressure in the operating cylinder is a feature advantageous for an initial fast reclosure in a duty cycle which initiates with an opening operation; second, that under the conditions of such an initial fast reclosure, trip-free operation has no advantage over nontrip-free operation.

Now, the fundamental concept of the principle of mechanically trip-free operation, which already has gained considerable recognition and acceptance in the field, is the infallibility of the circuit breaker opening performance that is inherent where those components of the operating mechanism that are potentially disruptive to that performance are functionally severed from the circuit breaker as every opening operation initiates.

In view of these controversial aspects of the grounds which they presented to justify departure from the principle of mechanically trip-free operation, in the case of the larger operating mechanisms, the authors may wish to elaborate upon the statements made in their paper.

Besides this thought several questions arise. The first question relates to the operation of the traveling trip-free point *C* of Figure 7 of the paper. During a normal closure, this point travels vertically a distance of $5^1/_2$ inches, corresponding to the piston stroke, and laterally some distance due to its rotation about point *D*. It appears that the inertia effect of this pin and its attached links might alternately load and unload the trip-free latching system rendering that system unstable and subject to spillouts. Has this been a problem and, if so, what has been done to overcome it?

The second question is concerning the number and type of adjustments. Selecting the trip magnet as an example, Figure 7 of the paper shows three adjustments:

1. Air-gap adjustment.
2. Kick-off spring adjustment.
3. Trip-rod adjustment.

All three are screw type. We would like to hear what locking means has been utilized to preserve these adjustments, once made. It has been our experience that screw-type adjustments will change under shock conditions even when locking means ordinarily considered effective are employed.

The third question: How is the force travel curve adjusted for high closing force at the end of the stroke without using a toggle or other multiplying system in the operating linkage?

The fourth and last question: Is a contact opening of less than 50 per cent full stroke on the first reclosing cycle considered safe under the most severe conditions of high interrupting capacity and rapid rate of rise of recovery voltage?

R. C. Van Sickle, W. T. Parker, and **F. E. Florschutz:** Both of the operating mechanisms described in the paper are mechanically trip free. Definition 4-59.1 of an American Standards Association Standard[1] states, "A circuit breaker is mechanically trip free when the tripping mechanism can trip it, even though: (1) in a manually operated circuit breaker, the operating lever is held in the closed position; or, (2) in an electrically operated circuit breaker, the operating mechanism is held in the closing position either electrically or by means of an emergency closing lever." The intent is to prevent an applied closing force from holding a circuit breaker closed against a fault even though the tripping circuit is energized. The smaller mechanism, whether operated pneumatically or manually for closing a circuit breaker connected to an energized line is mechanically trip free. The larger mechanism is trip free for pneumatic operation and the circuit breakers on which it is used require so much energy for closing that emergency manual closing is not practical because a man is not strong enough to close the circuit breakers on an energized line at a safe speed.

For the heavier applications of the small mechanism and all applications of the larger mechanism a ratchet-handle screw-jack type of hand-closing device is provided for maintenance operation only. The larger mechanism is normally not trip free for manual operation during maintenance. In fact, for safety during maintenance, both the smaller and larger mechanisms have a bar which can be inserted to prevent accidental release of the trip-free latches.

The links and pin at point *C* Figure 7 of the paper rotate about point *D*, and their mass requires a radial accelerating force as mentioned. But the links are light compression or tension members carrying heavy loads and their radial accelerating forces reduce the loads on the trip-free latches less than 1 per cent.

The adjustments of the trip magnet have been found to be very stable, possibly in part because the tripping does not depend upon impact and little impact is transmitted through the parts. The magnet is strong enough to release the latches by pushing and requires no impact to start them. The

armature is loaded after very little free travel and is stopped by lugs in the air gap. Little shock is transmitted through the trip rod because most of the weight of the moving parts of the trip is in the armature. The trip-rod adjustment is locked by an elastic stop nut which grips the rod and which is kept from turning by a locking clip. The lock nuts on the trip rod pick up the kick-off spring at no load during tripping and in the open position support the armature by transmitting the force of two relatively light springs and consequently little shock can be applied to them.

The force-travel-curve of the mechanism is fitted to the force-travel curves of the lift-rods of the circuit breakers by toggles and lever systems incorporated in the design of the circuit breakers. One mechanism may be suitable for the operation of several types of circuit breakers so the modifications are incorporated in the circuit breaker design.

The interruption of the current on the first reclosing operation takes place with a contact opening speed the same as on an opening which is not to be followed by a reclosure. The interruption takes place within 3 cycles, the transient recovery voltage appears across the contacts within an interval measured in microseconds, and it is replaced by the system frequency recovery voltage in about an additional half-cycle. As shown by Figure 14 of the paper, for an 11.5-cycle reclosure with contacts opening less than half of the full stroke, the compressed air to retard the opening and reclose the circuit breaker is not admitted to the cylinder until the end of 3 cycles and the opening speed of the lift rod is not retarded until after an interruption would have occurred.

Reference

1. Alternating-Current Power Circuit Breakers. *Publication Number C37.4*, American Standards Association, New York, N. Y., 1945.

Developing a Superspeed Trip-Free Reclosing Circuit-Breaker Mechanism

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FELLOW AIEE

H. L. PEEK
MEMBER AIEE

Synopsis: Circuit-breaker engineers are challenged by the demands for ever-increasing interrupting capacities of power circuit breakers, increases required as load growth and nation-wide system interconnections are planned. Within the past 10 years the maximum interrupting ratings of high-voltage circuit breakers has tripled and the ceiling is not yet in sight. These high interrupting capacities represent substantial increases in the duty imposed on the operating mechanism, for the magnetic forces alone imposed on the contacts of a circuit breaker multiply as the squares of the short-circuit current.

Modern operating requirements which add to the duty of the circuit-breaker operating mechanism are the requirements for circuit clearing in 3 cycles and repetitive reclosing in intervals as fast as 15 cycles. These necessitate the control of accelerating and decelerating forces considered extreme a few years ago.

With these new demands for operating requirements is a matching development in circuit breakers. This paper describes the development and design of a new operating mechanism offered to meet the advanced specifications of high-voltage circuit breakers. As specified, this mechanism is required to control the circuit-breaker contacts for circuit clearing in 3 cycles. Successive reclosing operations are to be completed in intervals as short as 15 cycles without intentional time delay. The operating mechanism continues to be mechanically trip-free during any operation and is suitable for application to the largest tank-type circuit breakers now listed in the standards.

The methods used for satisfying these requirements are described. Considerable emphasis has been placed on the procedures used in testing and proving this new design. The value of modern instrumentation in establishing valuable measurements and other engineering data is an important part of this paper.

Paper 52-170, recommended by the AIEE Switchgear Committee and approved by the AIEE Technical Program Committee for presentation at the AIEE Summer General Meeting, Minneapolis, Minn., June 23–27, 1952. Manuscript submitted March 26, 1952; made available for printing April 22, 1952.

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EXPERIENCE in the design and manufacture of circuit-breaker operating mechanisms, reinforced with experience in the field, provides the engineer with certain principles of design that are almost basic when applied to circuit breakers. These include:

1. Make it simple and reliable.
2. Load members in compression or tension.
3. Use compression latches with cylindrically ground faces.
4. Limit the mass inertia of moving parts.
5. Use shim instead of screw adjustment.
6. Cushion against harmful vibration at its source.
7. Use rolling instead of sliding surfaces.
8. Consider the detrimental effects of exposure.
9 Make it easy to inspect and maintain.

This list is far from complete and must be accepted as guiding principles, not mandates, for compromise is necessary occasionally. Success of design then, can be measured partially by adherence to this list.

For a mechanically trip-free operating mechanism a 4-bar linkage has proved reliable and simple. The kinematic arrangement, see Figures 1 and 10, illustrates how this 4-bar linkage can be arranged to provide:

1. Reduced tripping force through a toggle reduction.
2. Increased force near closed position through another toggle.
3. Trip-free operation through a simple latch.
4. Loading of the majority of links in compression.

The 3-cycle operating time required of the associated circuit breaker, demands that the bayonet type contacts within the interruptors must part contacts within a time interval of $1^1/_4$ cycles after energizing the trip coil. This specification was made more stringent by limiting power input to the trip coil to existing values of 2,000 volt-amperes. The extensive experience with conventional shunt trip solenoids recommended their use because of their reliability and simplicity. Initially it was realized that much would be gained were it possible to incorporate this simple trip-coil arrangement for all high-speed trip operations. This objective was achieved by using a small lightweight and balanced secondary latch engaging a primary prop latch. The primary prop latch acted as a temporary block to the fourth link of the 4-bar linkage. High-speed latch disengagement was achieved by grinding the primary prop latch about a center displaced from its rotational center. The vertical force component of the fourth link, acting against this displaced arm, provided a controlled tripping force against the final trip latch. Careful design of the trip solenoid, together with the mechanical refinements of the simple trip linkage, furnished the high-speed disengagement required. The need for critical adjustments of all prop latches and rollers is omitted by using

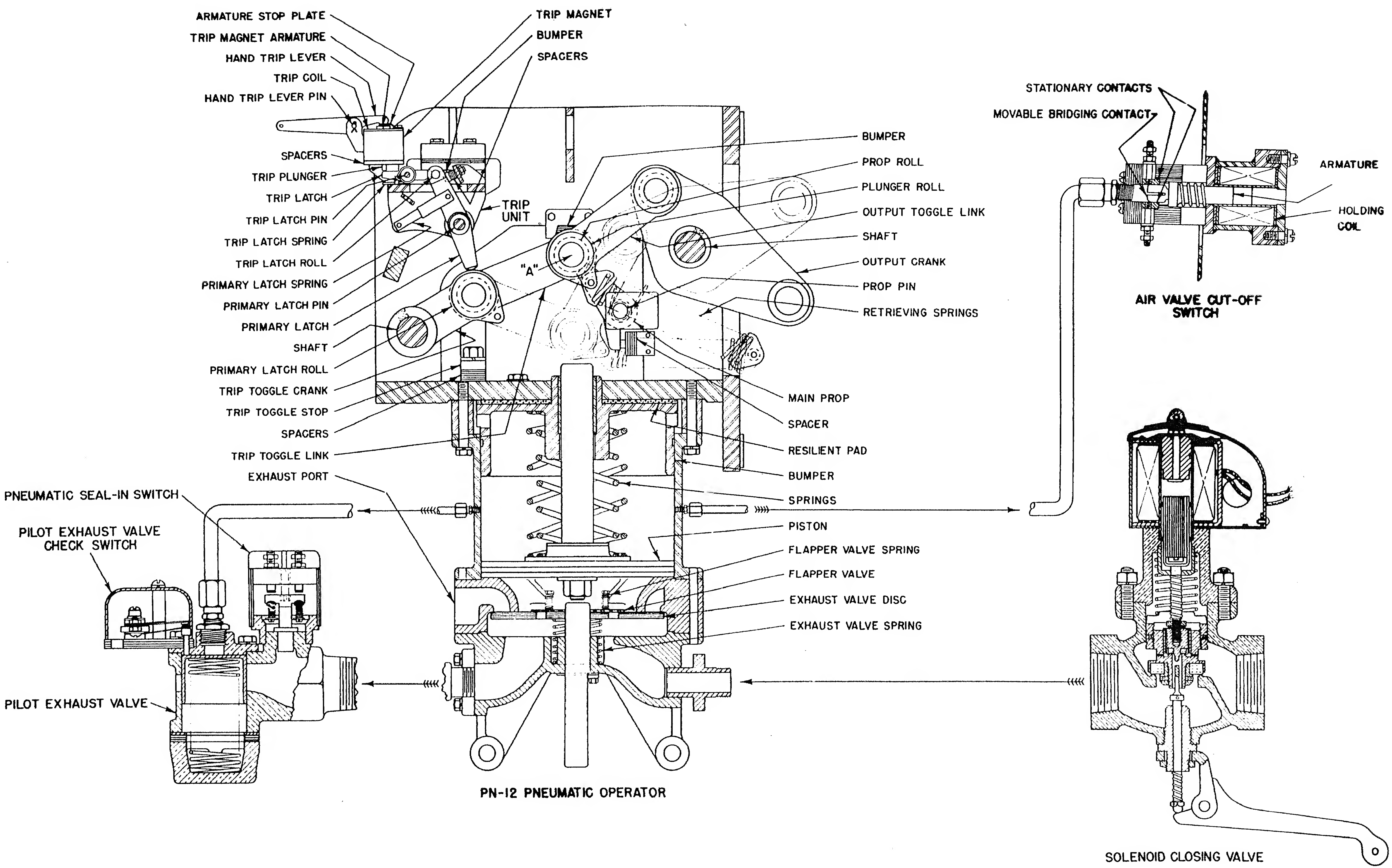


Figure 1. Cross-section drawing of the type PN-12 operating mechanism. Shown in the closed position, phantom lines show open position of linkage. Pneumatic control auxiliaries are illustrated at double the scale of the mechanism

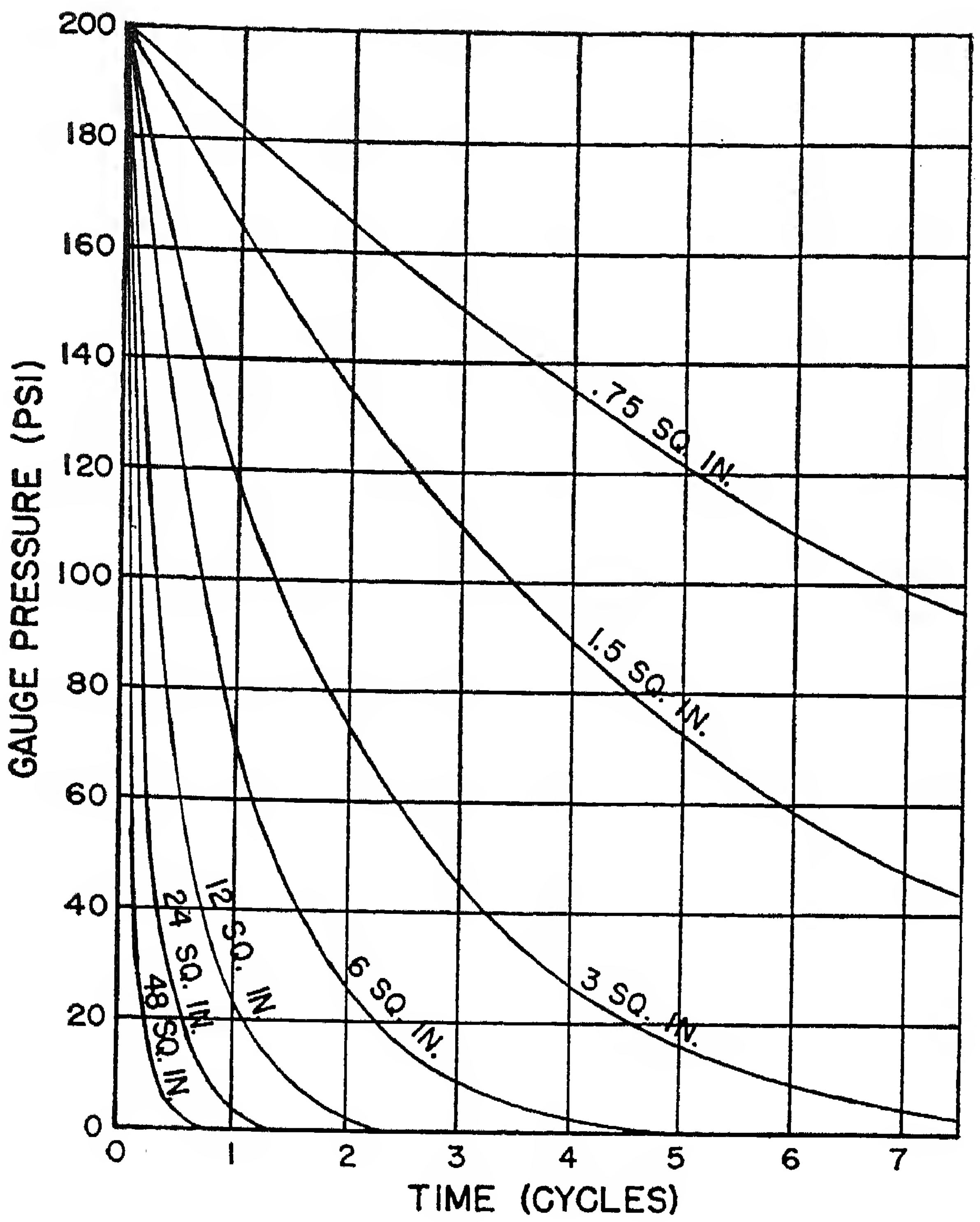


Figure 2 (left). Calculated air pressure decay curves of a 565-cubic-inch cylinder. The effect of various exhaust opening areas is shown. An orifice factor of 0.6 was used

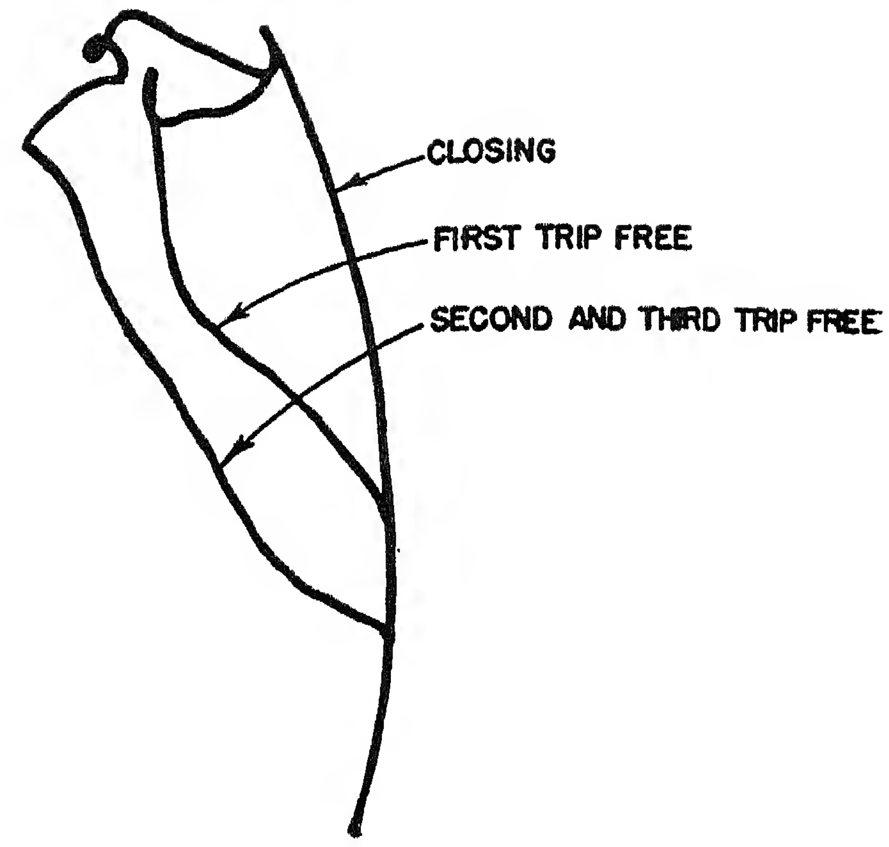


Figure 3 (right). Paths of toggle pin A from Figure 1 or Figure 10

machined surfaces or solid shims for engagement stops.

This mechanically trip-free linkage, simple and reliable in itself, contributes much to the reliability of circuit breaker operation. Actual mechanical motions, whether tripping from the closed position or tripping free while closing, differ only in degree and in the time required to recouple and relatch. When tripping from the closed position, the piston ram is at the bottom of its stroke. Pin *A*, Figure 1, has only to roll free of the closing prop as the linkage collapses. Powerful reset springs then recouple the linkage and a reclosing operation can be initiated as soon as the latch check switch indicates re-engagement of the trip latch.

Tripping free during a high-speed reclosing operation, this same pin *A* rolls off of the face of the piston ram, then waits for the piston ram to approach its bottom position to recouple the linkage and trip latch. The latch check switch again indicates trip latch engagement and invites another reclosing operation.

The similarity in these opening operations makes it possible to use a single and simple trip coil, energized through a conventional control circuit. A transfer of control circuits to other trip coils or to solenoid valves is unnecessary for either tripping or trip-free operation.

A 15-cycle reclosing operation immediately after a prior reclosing operation presents a real problem; that of returning the piston and piston ram to the fully open position in the least possible time so that the linkage can recouple and drive the circuit-breaker contacts closed again. Calculations showed that the piston and piston ram should return to the open position in less than 3 cycles in order to achieve this high-speed mechanism recoupling and circuit breaker reclosing without delay. Piston return could only be achieved after the operating cylinder dumped its load of compressed air. The springs acting on the piston could return it, once air was dumped, in 2 cycles. The problem was, therefore, one of discharging as much as 8,000 cubic inches of free air from a cylinder of about 600 cubic inches of displacement in 1 cycle. Figure 2 illustrates the time characteristics of various exhaust openings when dumping air at 200 pounds per square inch from this cylinder. In reviewing this curve it was apparent that at least 24 square inches of exhaust area would be required and that the initial operating pressure had little effect on this time. A solenoid valve with 24 square inches of exhaust area would be prohibitive and conventional dump valves quite impractical. Design experience with certain air-blast circuit breakers where the air is dumped from operating cylinders through a false cylinder bottom offered a practical solution. Figure 1 illustrates the cylinder arrangement used on the *PN-12* for high-speed air exhaust. An intermediate casting between the cylinder and the lower cylinder head is arranged with an annular opening near its outer wall. A light exhaust valve disk closes this annular opening to seal the cylinder from the outside. Compressed air, after entering the lower casting, flows through a number of holes in the valve disk and forces the piston upward.

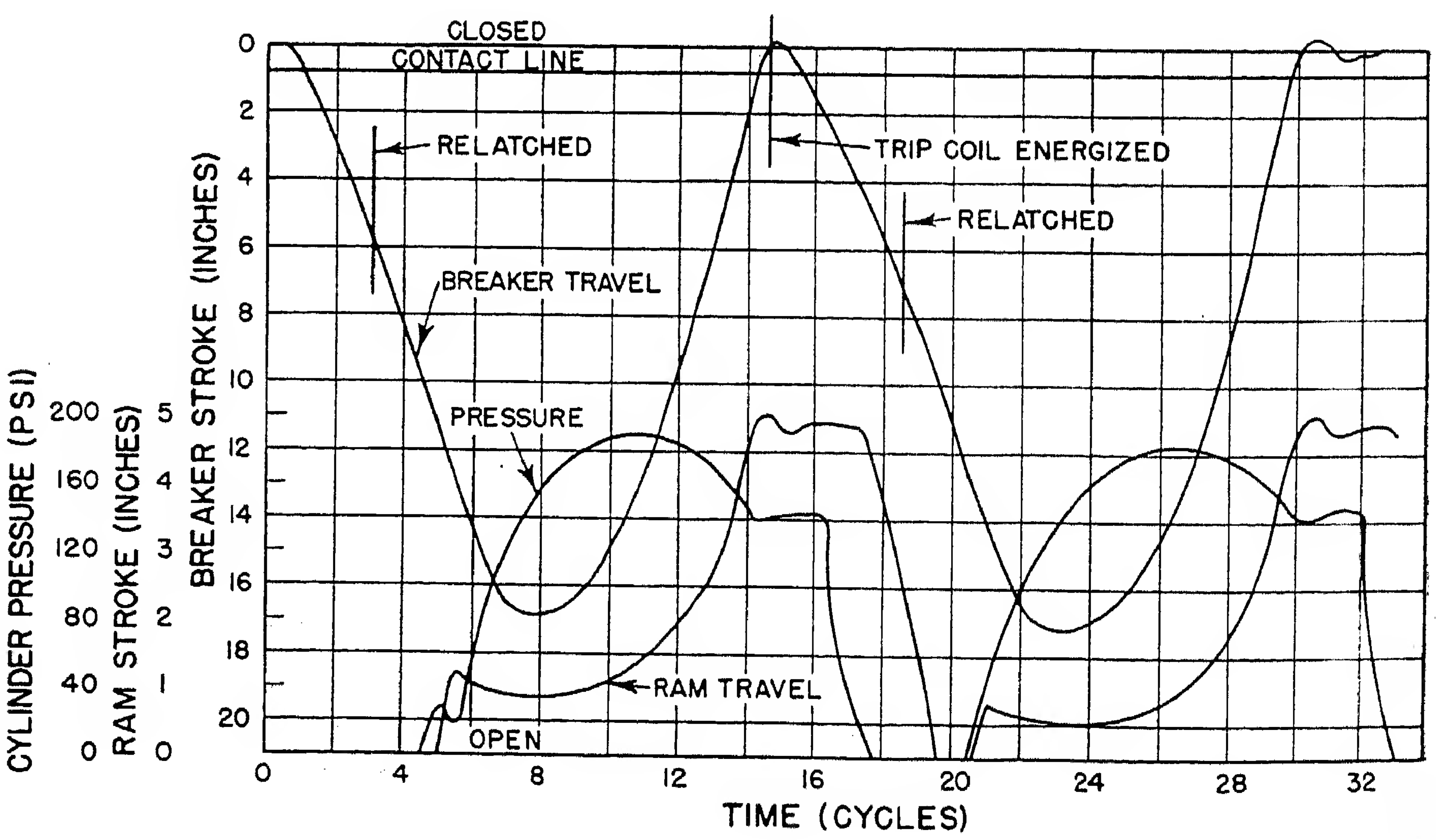


Figure 4. Time sequences of various operations during two successive reclosing operations of a 3-pole 161-kv oil circuit breaker with a PN-12 operating mechanism

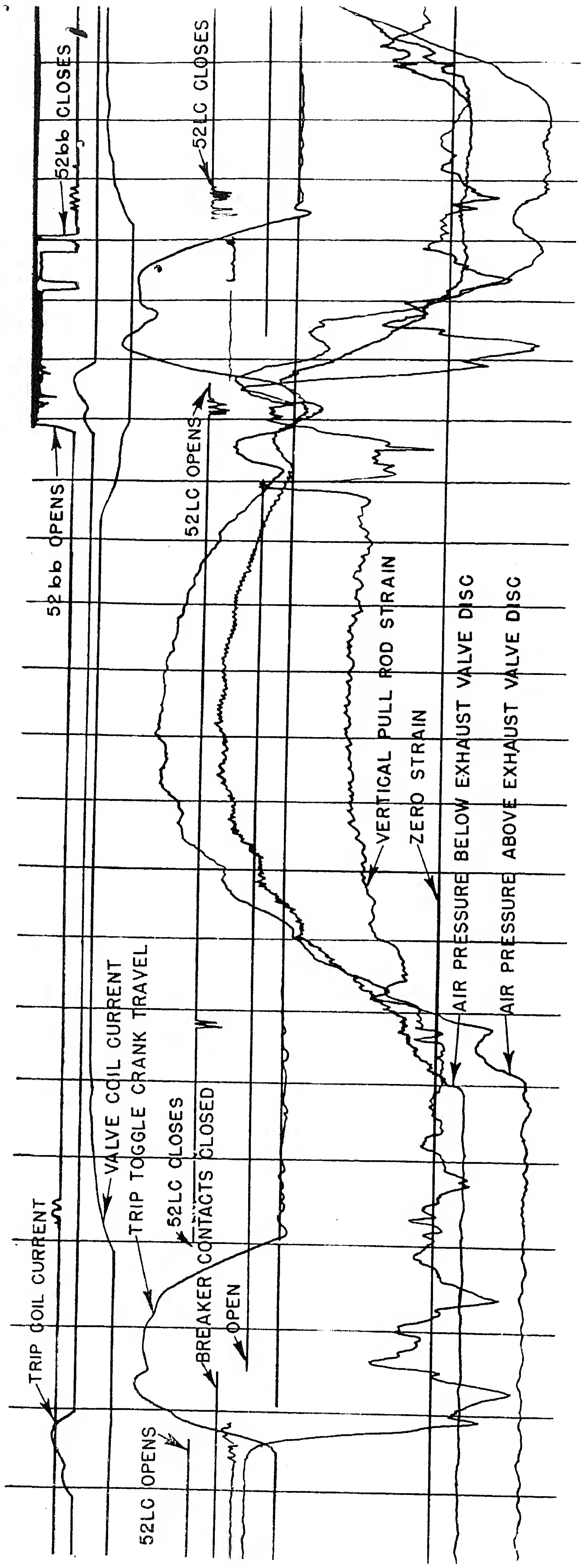


Figure 5. Portion of a typical 3-reclosure oscillograph showing changes of pressure, force on pullrod, coil currents, and motion of trip toggle crank with time. Also shown are the timings of various control elements. A mechanically operated air valve cutoff switch of the contactor type equipped with magnetic blowout and arch chute was employed in this test. Contrast the $1^1/_2$ cycles required to de-energize the valve coil with the 0.2 cycle required by the air-operated air-valve cutoff switch as shown in Figure 10

When the flow of air is cut off a small pilot exhaust valve is opened, and the decay of air pressure in the lower head casting forces the exhaust valve disk downward. The air in the operating cylinder then exhausts through the 24-square-inch annular opening uncovered by the valve disk. A smaller disk valve prohibits a return flow of air through the holes in the exhaust valve disk.

Figure 1 also pictures the final operating mechanism assembly and emphasizes its breakdown into subassembly units. The entire trip mechanism is an interchangeable subassembly with no need, or provision, for, critical adjustments. The mechanism frame carries the sturdy 4-bar linkage and reset springs. Mounted to the bottom of the mechanism frame is the operating cylinder assembly which includes the high-speed dumping arrangement, heavy piston reset springs, and a special buffer design that stops the piston against a resilient pad at the end of its closing stroke. Air controls include a standard solenoid valve for admitting air to the cylinder and a conventional low-capacity dump valve that functions as a pilot for the cylinder dump disk.

Unique to this application are two special air-operated controls. The first is the air-valve cutoff switch, see Figure 1, which functions as a small air-blast circuit breaker to de-energize quickly the solenoid valve coil at circuit breaker closed position. It is equipped with a holding coil that holds the switch open, once the air blows it open, until the main control circuit is de-energized, thus providing the antipumping function conventionally performed by a *Y* relay. The second is a pneumatically operated seal-in switch, see Figure 1, that seals in around the closing control contacts as soon as pressure enters the cylinder. This not only provides for completion of a closing operation, once started, but also prevents sealing in the solenoid air valve when no pressure is available at the valve. This switch, by its seal-in action, provides positive flip-free operation. The solenoid air valve for closing requires a modest value of current. These two conditions make possible, and very desirable, the omission of the *X* relay. Hence, neither the *X* nor *Y* relays are used with this operating mechanism.

Design Tests

In the development of a new piece of apparatus, good engineering practice requires that a prototype of the product be tested under the most severe operating conditions. A detailed test schedule was prepared to provide the engineers with a maximum of information during this test program. In addition to performance tests, life tests, maximum loading tests, trip-free operations, and so forth, engineering information was to include the following:

1. A measurement of transient stresses of selected areas in the mechanism under the transient conditions of repetitive reclosing after trip-free operations.
2. The measurement of the transient

pressures within the air system during operations.

3. Transient stresses throughout the circuit-breaker linkage.

4. Accelerations and velocities of various moving parts.

5. A record of the true paths of motion of the 4-bar linkage during closing and tripping.

The instruments selected to make this study included:

1. A 12-element magnetic oscillograph.
2. Multiple-channel amplifiers.
3. Resistance-type strain gauges.
4. Resistance-type pressure recorders.
5. Superspeed motion picture camera, 500–3,000 frames per second.
6. Special resistance-type rotary travel recorder for trip toggle crankshaft.
7. A special drum and pencil-type travel recorder for circuit breaker movable member, see Figure 12.
8. A special device for determining paths of motion of 4-bar linkage.

History of Tests

During the early tests some difficulty was experienced in latching the circuit breaker closed after a reclosing operation. The superspeed movie camera supplied a visual record of unpredicted mechanical motions. The props used to hold the closing toggle closed were found to bounce from their stops and failed to accept the load of the closing toggle when the piston ram started its return stroke. Metallic buffer stops for the props eliminated this bounce and cured the trouble. The test program was then extended to include superspeed movies of all areas where unusual motions or stresses were involved.

Figure 14 pictures the final arrangement of the mechanism and shows the closing prop, the main closing toggle, the primary latch, and the piston rod. An opening has been cut in the main frame to expose the primary latch. A resistance-type rotary travel recorder coupled to the fourth and tripping link shaft was used for oscillograph records. Figure 5 shows a typical oscillogram that includes control operations, mechanical motions, certain strain measurements, and pressure values. Figure 3 illustrates, on a small scale, the travel of the toggle pin that carries the piston ram roller for closing, first trip free, and subsequent trip-free operations.

During these tests, the mechanism was coupled to a triple-pole tank-type circuit breaker rated at 161 kv 7,500,000 kva. This circuit breaker incorporates new interruptors that depend on heavy impulse springs working during the last several inches of closing motion. Figure 4 is a

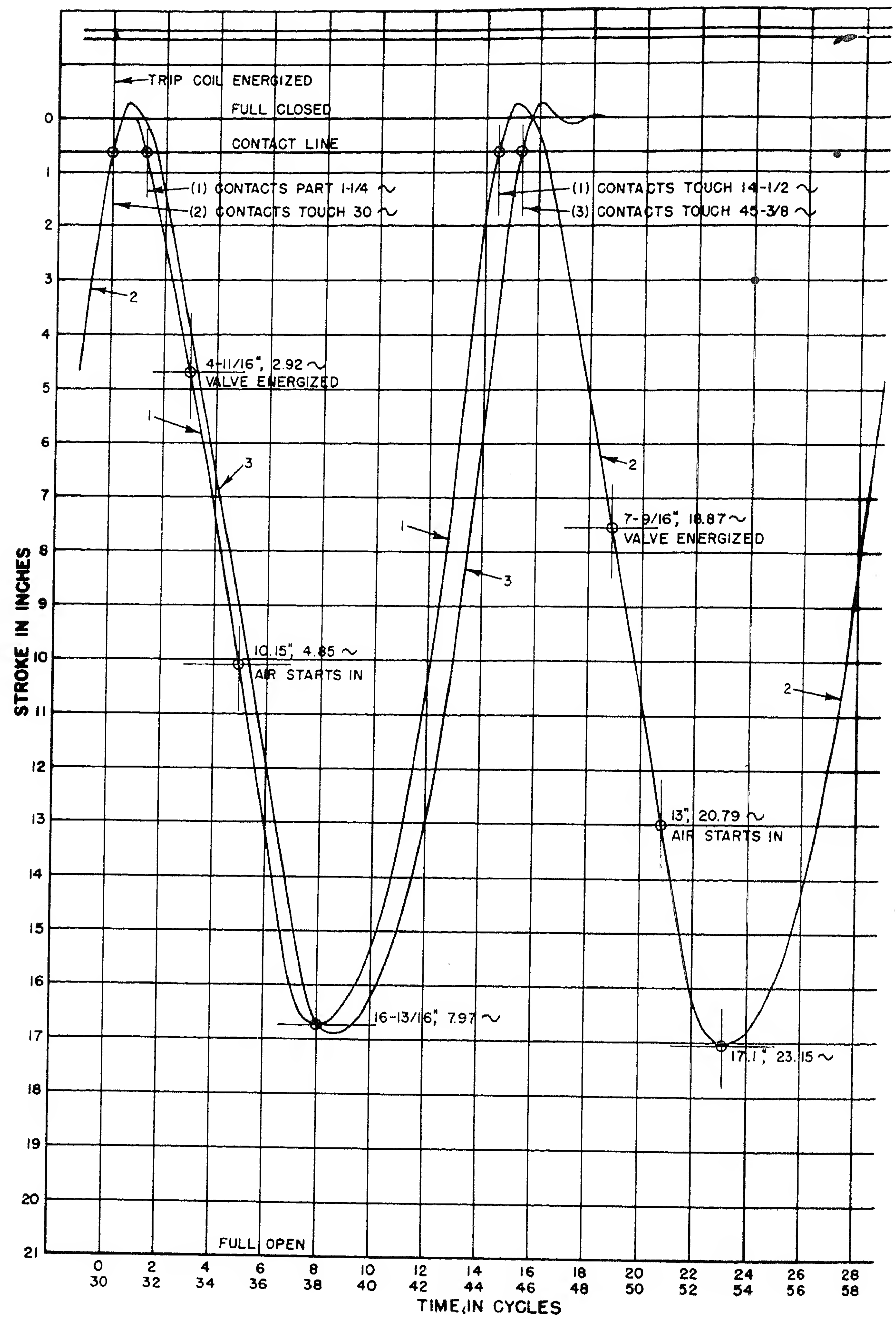


Figure 6. Speedgraph taken simultaneously with oscillograph of Figure 5

record of two reclosing operations of this circuit breaker by the *PN-12* operating mechanism. This chart records contact parting $1^1/_4$ cycles after energizing the trip coil. It shows that the mechanism has relatched in approximately 3 cycles and that the contacts have reversed their motion after 17 inches of movement and are recontacting in slightly more than 14 cycles. Pressure build-up in the cylinder and piston ram travel are shown. The second reclosing operation is completed in a fraction more than 15 cycles. Other reclosing operations might have been carried on until minimum pressure in the storage tank locked the control circuits open.

Figure 6 is a reproduction of a record made by the special travel recorder and shows 3 complete operations where the circuit breaker starts and ends in the closed position.

Once the early adjustments and modifications of shakedown tests were completed a 3-pole 161-kv 7,500,000 kva circuit breaker and operating mechanism was turned over to the test and inspection organization for an extended life test. This design life test included:

1. Three thousand normal closing and

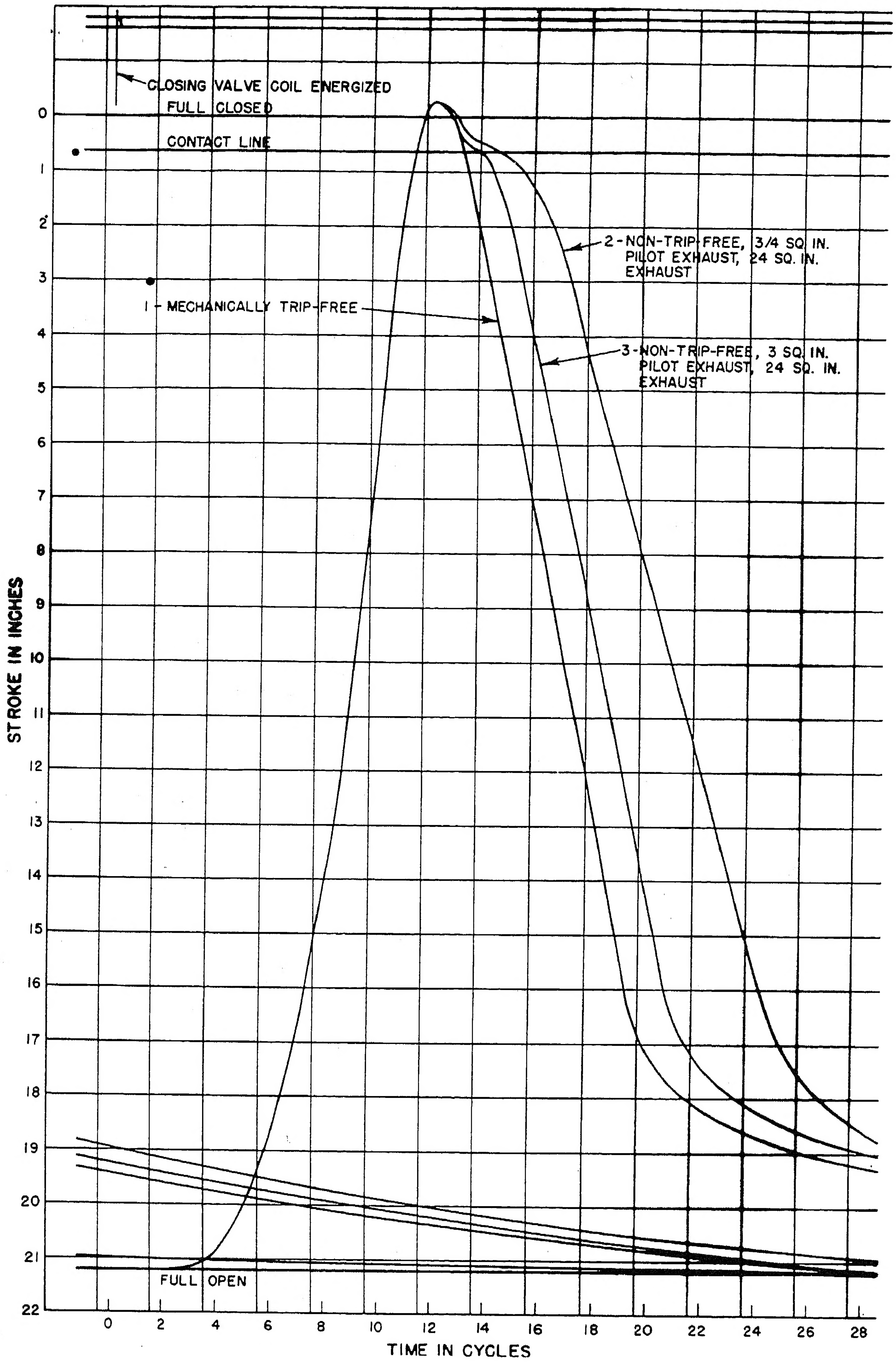


Figure 7. Speedgraph of close-open operations illustrating difference in speed between mechanically trip-free and nonmechanically trip-free circuit breakers

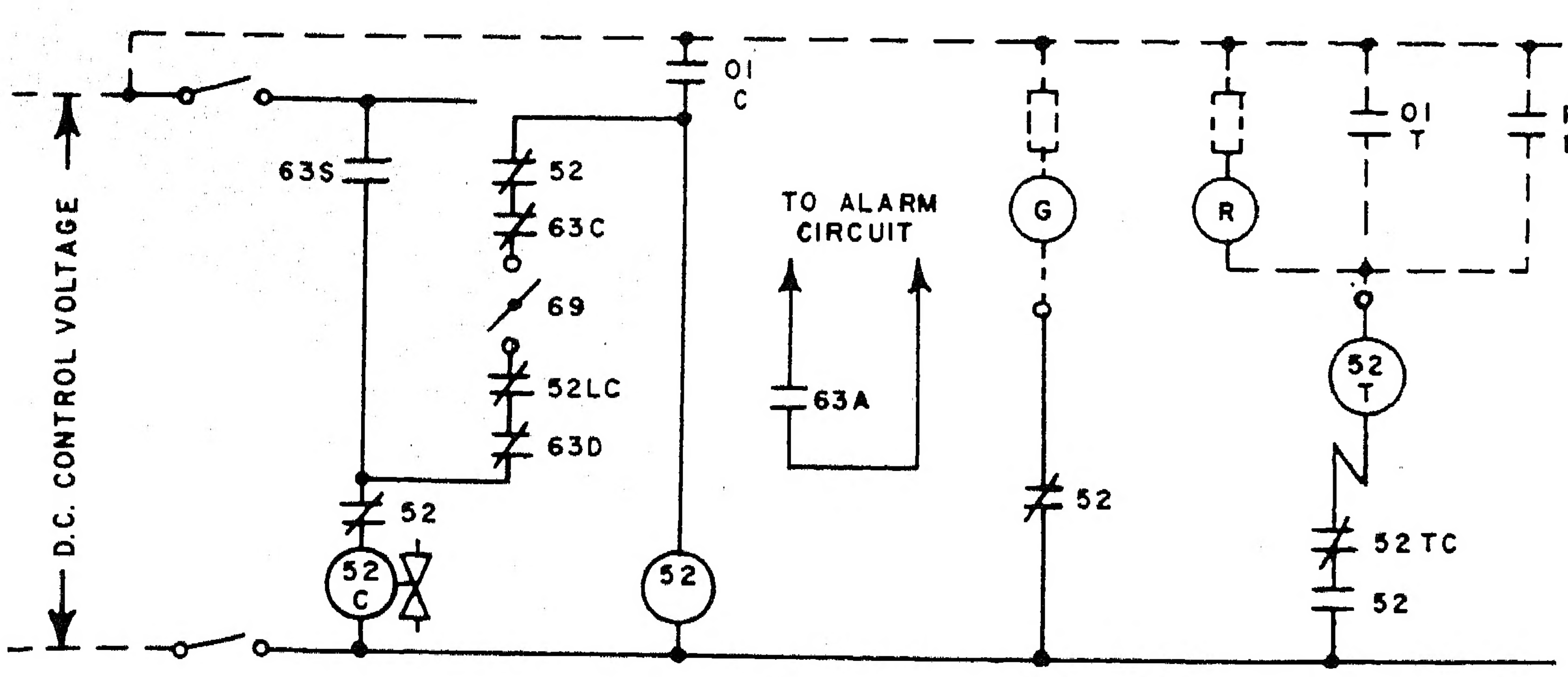


Figure 8. Schematic control diagram for PN-12 pneumatic operator. Conventional X and Y relays have been omitted as unnecessary

normal opening operations at maximum pressure.

2. Two hundred duty-cycle operations.

3. Three hundred 4-shot instantaneous reclosing operations.

4. Five hundred mechanical trip-free operations (trip coil energized by the making of the circuit-breaker contacts).

5. Twenty trip-free operations from the hand-closing jack.

After this total of 4,000 operations was completed, miscellaneous tests such as early trip-free operations, operations under maladjustments, low pressure, and so forth, were made. All tests were satisfactory.

Figure 7 offers some strong evidence of the advantages of a mechanically trip-free mechanism. Three opening curves are shown:

1. Where the circuit breaker was opened mechanically trip free.

2. Where the circuit breaker was opened pneumatically trip free by operating the 24-square-inch exhaust through a three-quarter-square-inch pilot exhaust.

3. Shows a similar operation where the pilot has been increased to 3 square inches.

Both of the pneumatically trip-free operations suffer a delay in contact parting while air is being exhausted from the cylinder. The mechanical trip-free operation effectively uncouples the circuit breaker linkage from the operating piston. Circuit-breaker contacts are free to open at their normal speed without regard to piston position. No dependence is placed on any portion of the air system or the compressed air itself for this trip-free operation.

Summary

By providing a simple and large area air exhaust at the operating cylinder, the advantages of a mechanically trip-free mechanism are applied to superspeed reclosing circuit breakers. Simple arrangement of a single shunt-trip solenoid and compression latches operates with sufficient speed to insure circuit interruption in 3 cycles. A combination of pneumatic and electrical operations has resulted in simple and reliable control operations. Modern instrumentation has been of real value in testing and proving this operating mechanism under the extreme conditions of high load and high speeds. Transient stresses, pressures, and motions are a matter of record. Superspeed movies have disclosed reactions unsuspected during the early design stages. The initial operating record of this operating mechanism has been gratifying.

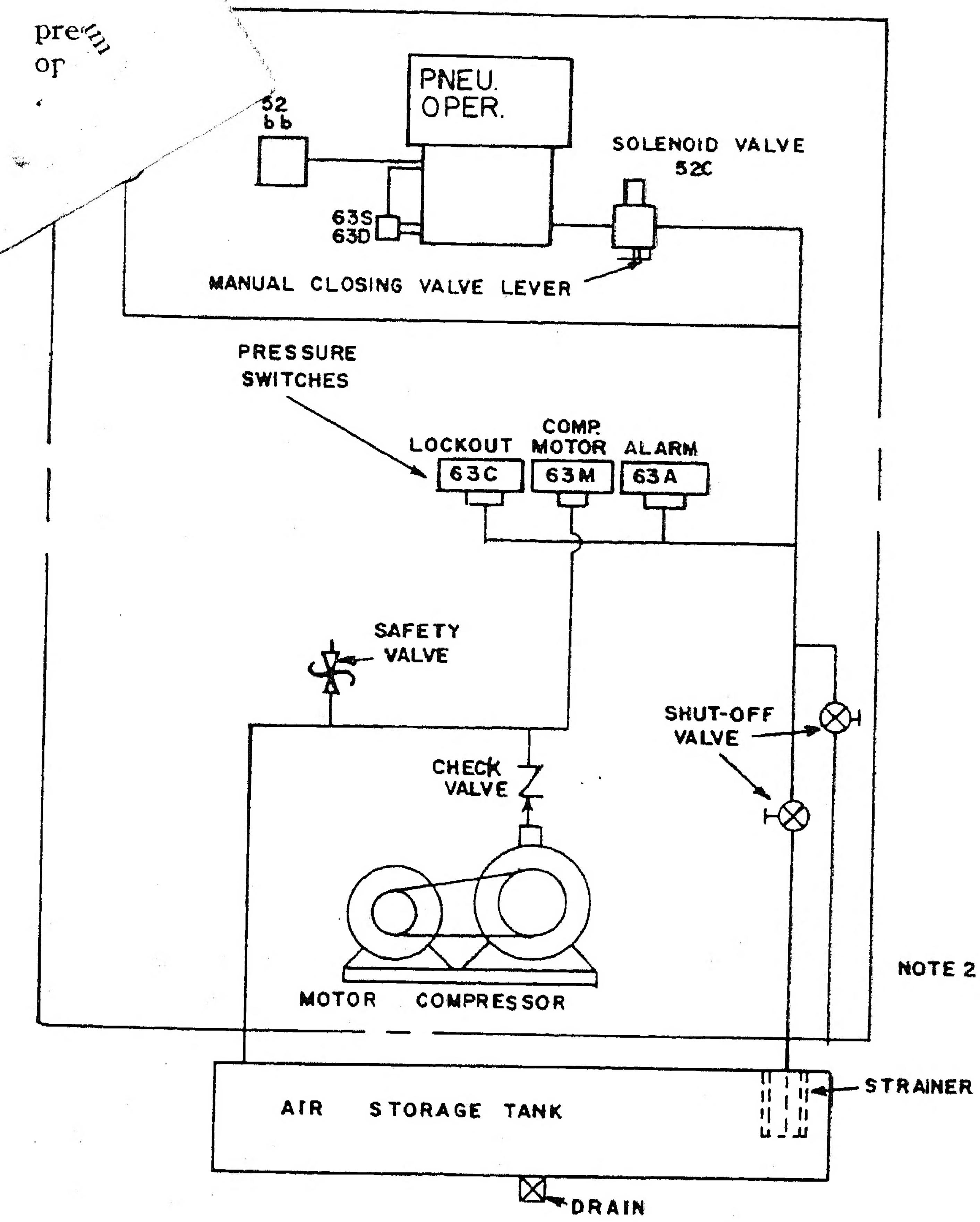


Figure 9 (left). Air system piping diagram. The construction, by avoiding water pockets, provides for complete drainage of any moisture into the storage tank

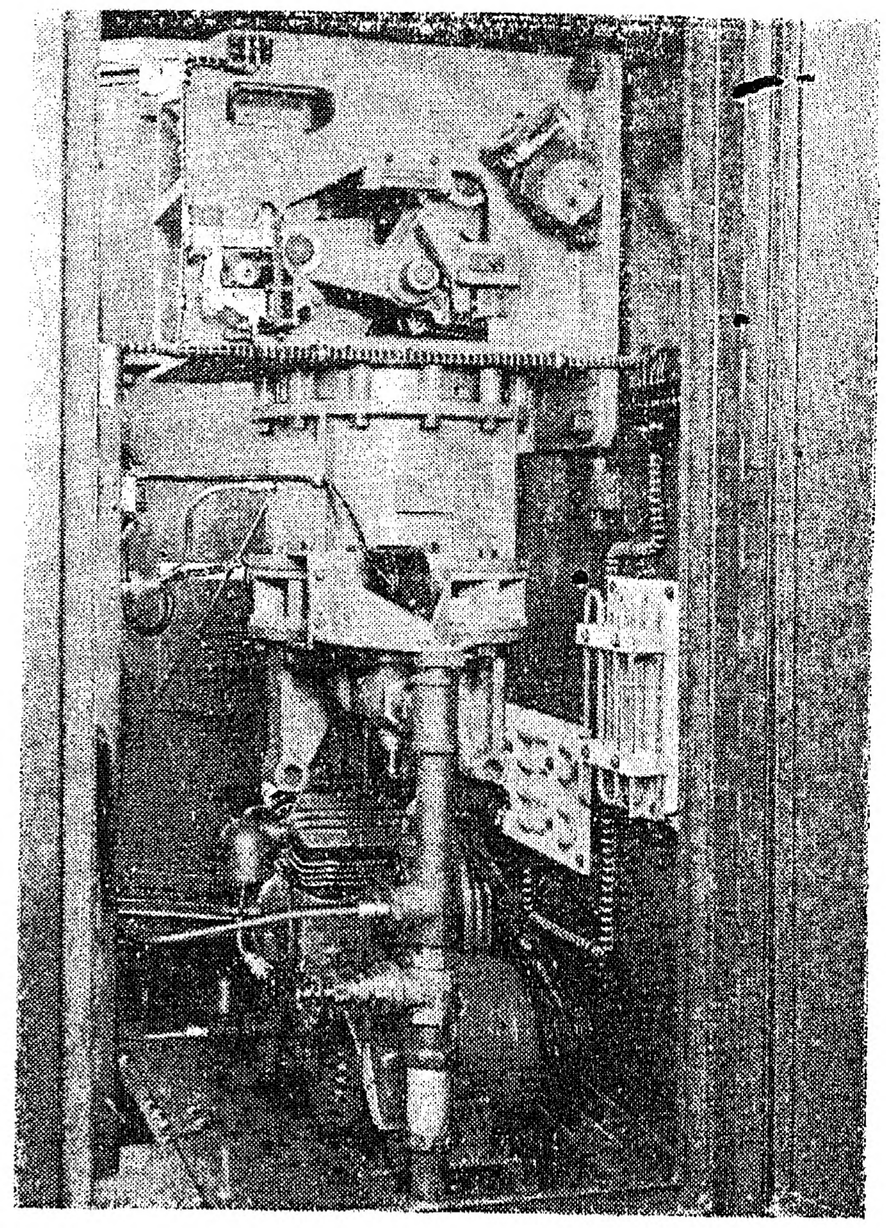


Figure 11. Right-hand side view of operating mechanism showing arrangement inside the cabinet

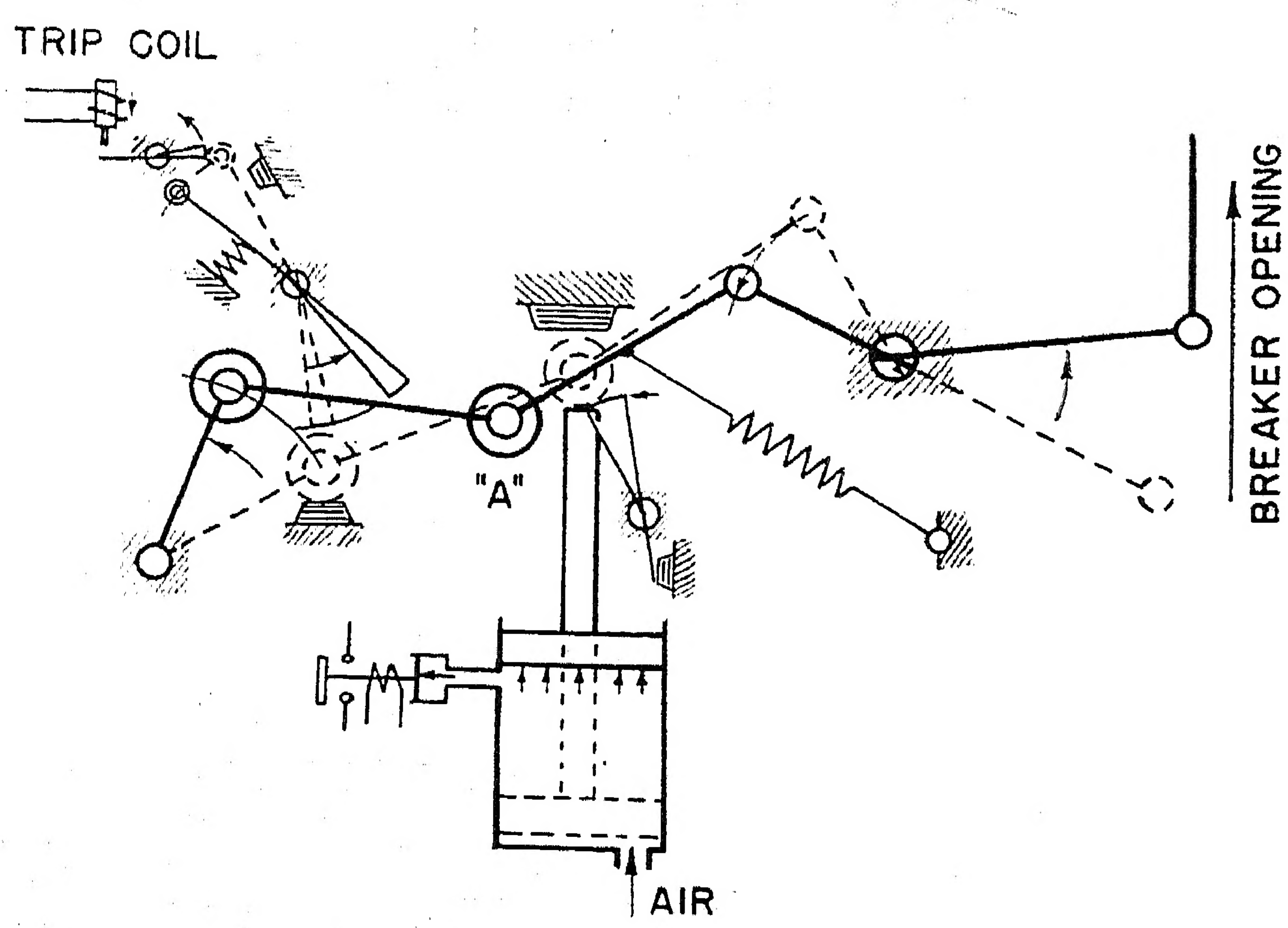
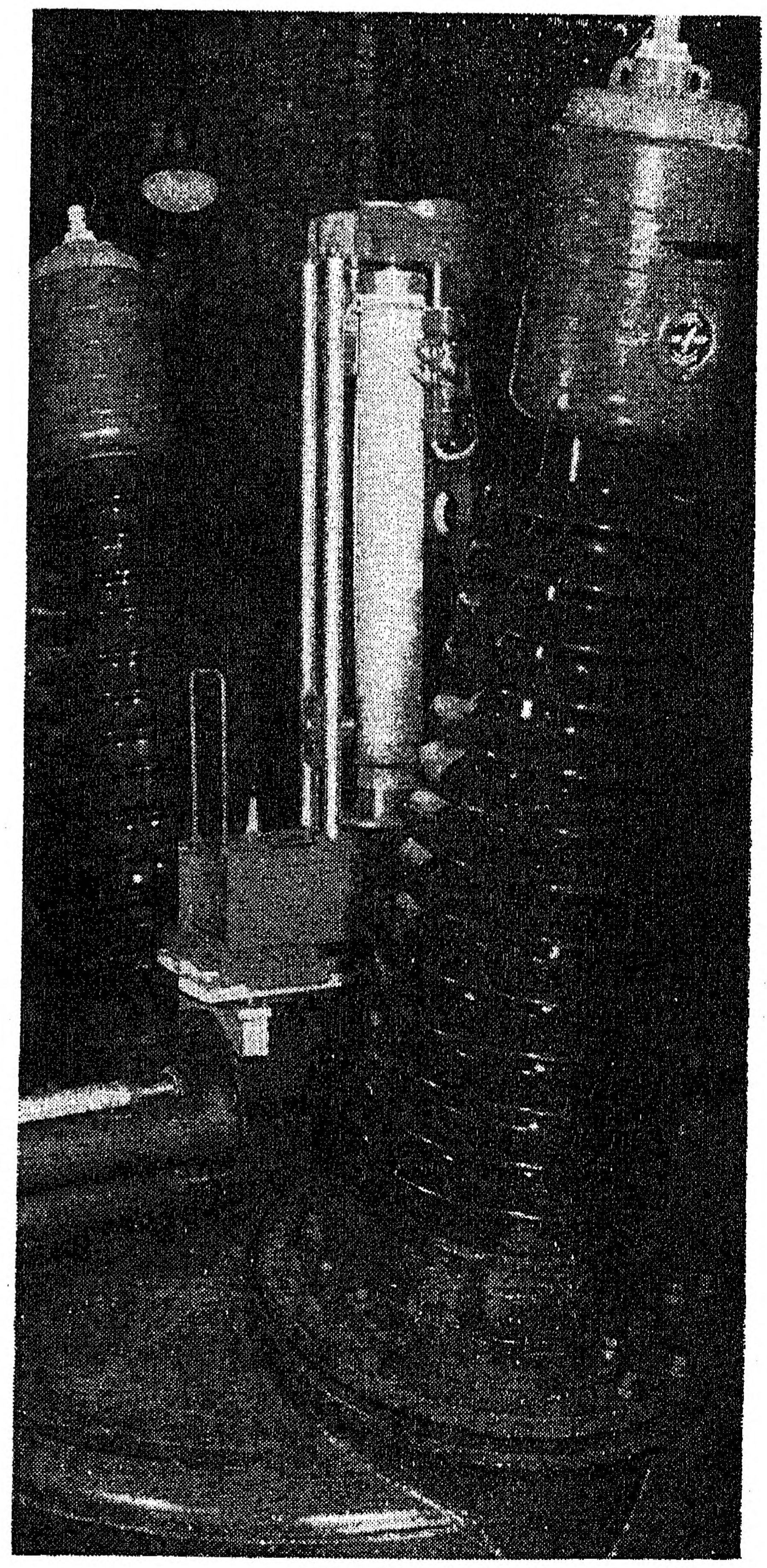


Figure 10 (left). Mechanical-linkage diagram illustrating the tripping or trip-free action of the operating mechanism



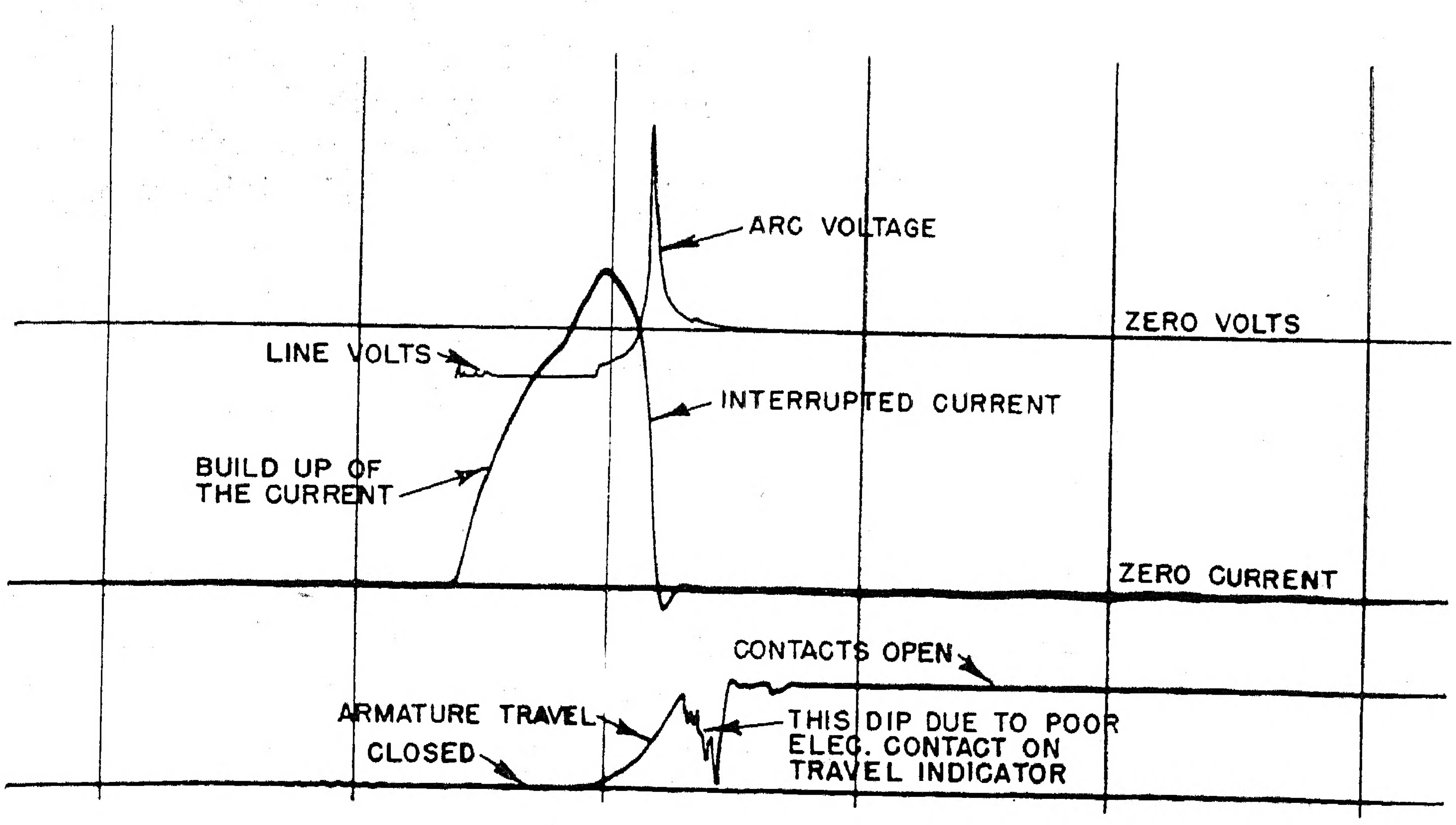
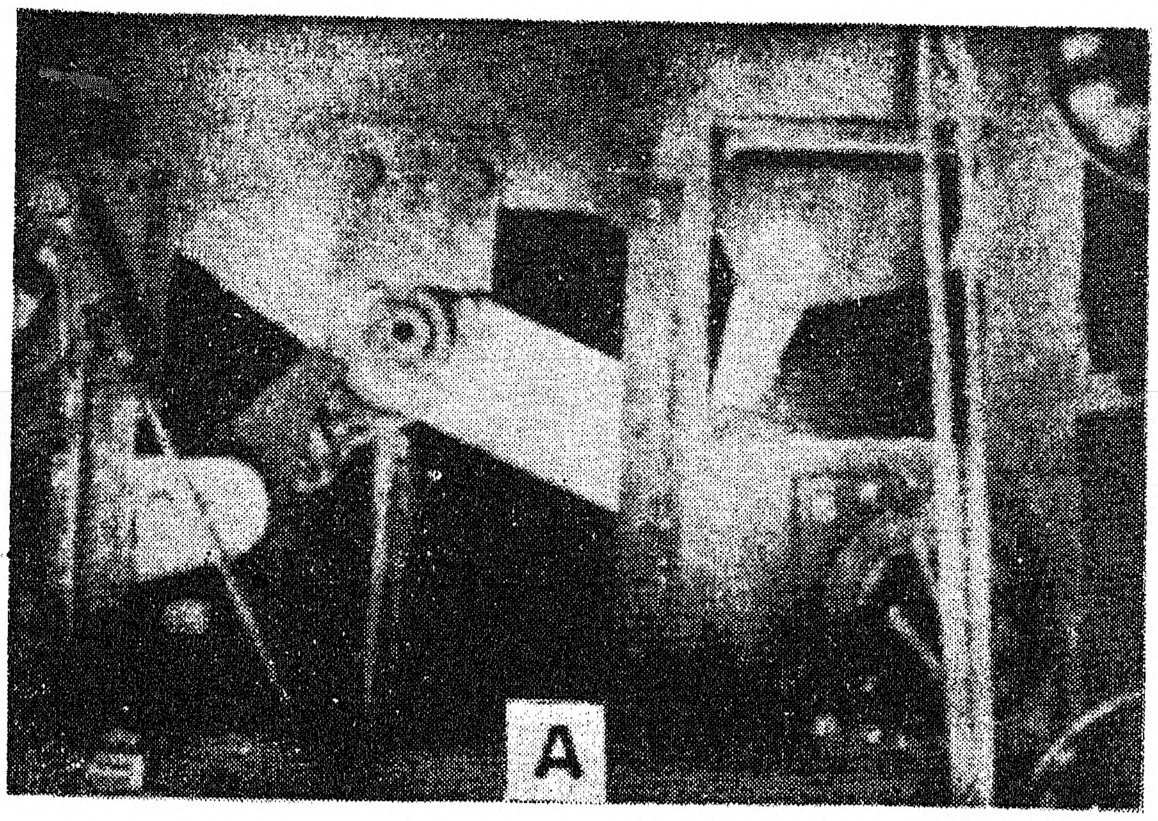
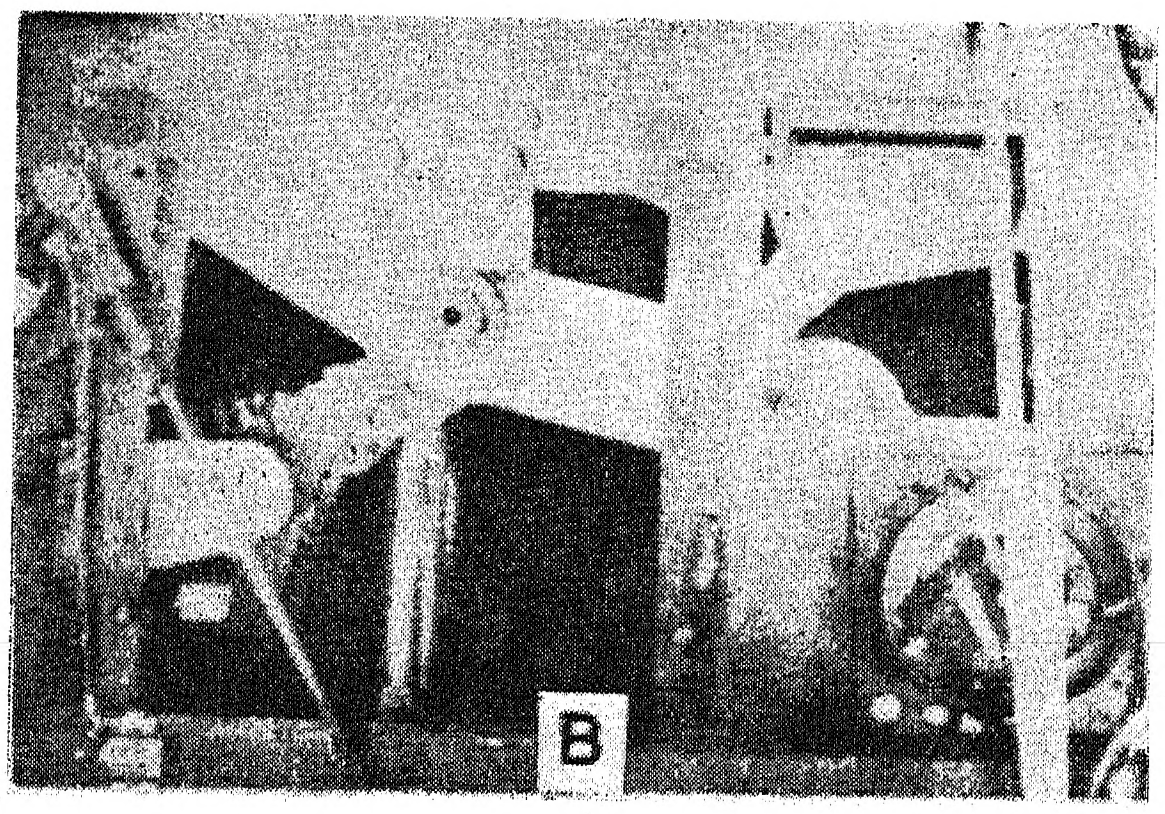


Figure 12 (above). Graphic speed analyzer used to obtain full-scale circuit breaker travel in cycles of time

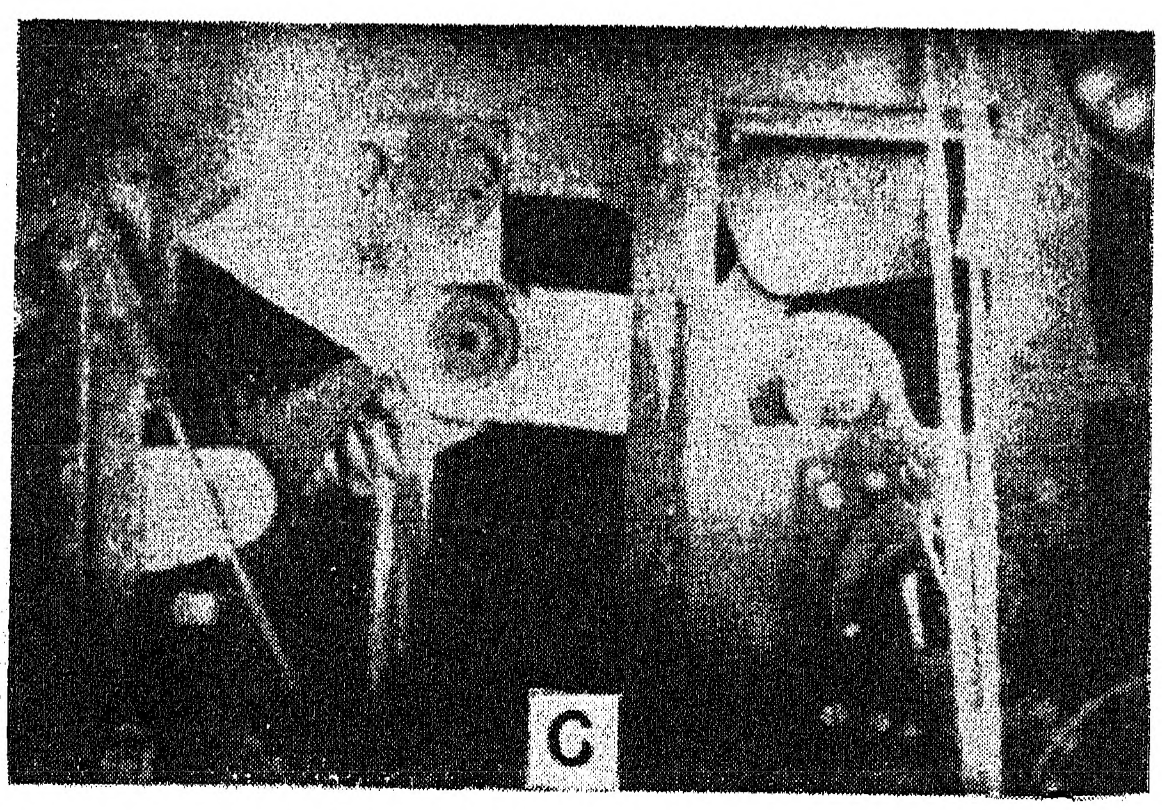
Figure 13 (left). Oscillograph taken during development of pneumatically operated air-valve cutoff switch. Air-valve current is interrupted in 0.2 cycle



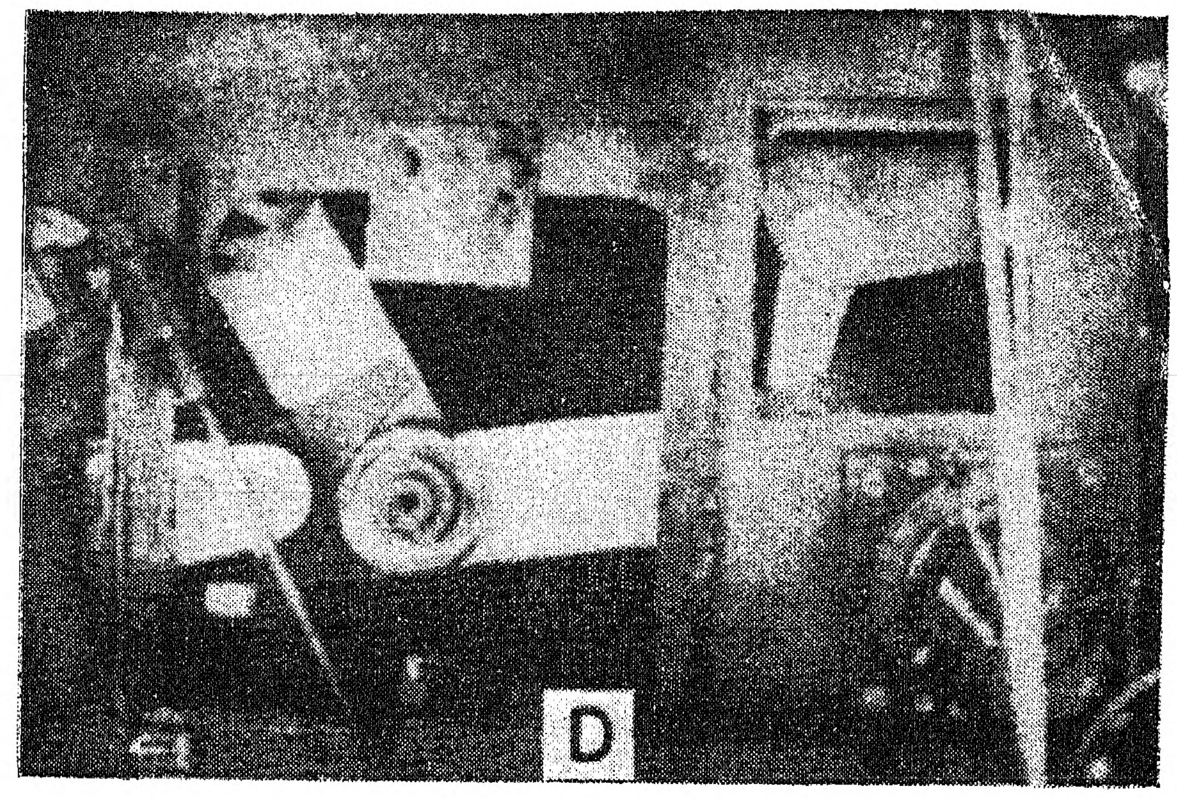
Mechanism approaching closed position at end of first reclosure. Main prop beginning to fall under prop roll (14$^3/_4$ cycles after first trip)



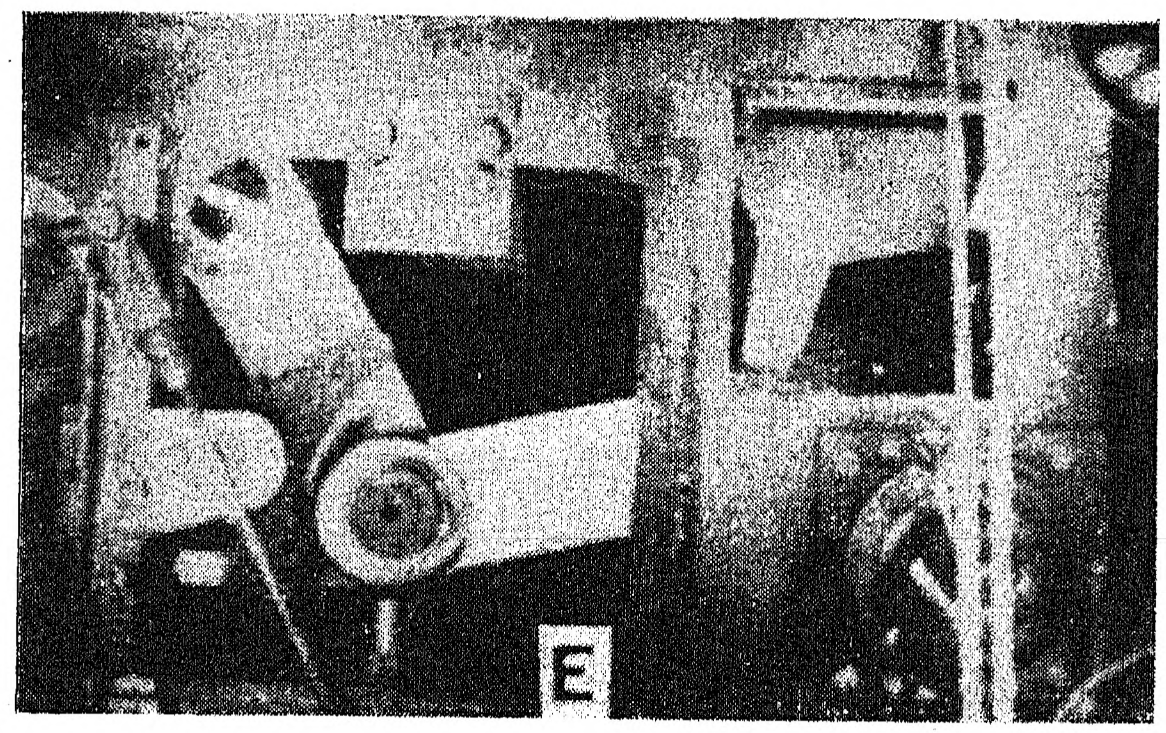
Mechanism reaches closed position and is tripped free for second opening, 1/2 cycle later (1/4 cycle after second trip)



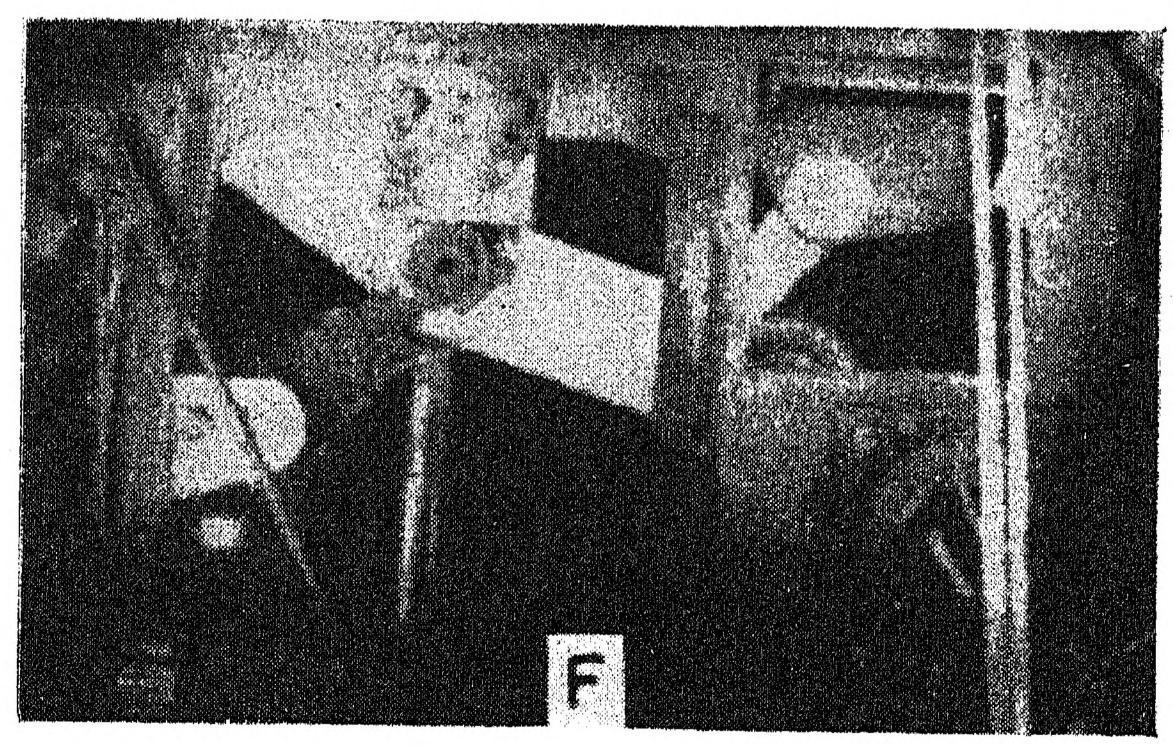
Ram begins to return. Note clearance beginning to open up between roll and over-travel stop, 1/4 cycle later (1$^1/_2$ cycles after second trip)



Ram at bottom of stroke, 2$^1/_2$ cycles later (4 cycles after second trip). Note that it would take approximately 2 cycles for the mass of ram and piston to be retrieved if no air were under piston



Ram again hits roll, 2$^1/_2$ cycles later (6$^1/_2$ cycles after second trip)



Circuit breaker all the way closed second time and being tripped for third opening, 10 cycles later (16$^1/_2$ cycles after second trip, 31$^1/_2$ cycles after first trip)

Figure 14. Blown-up frames from high-speed movie film chosen at selected intervals during second reclosure of an open—15-cycle close-open—16$^1/_2$-cycle close-open—16$^5/_8$-cycle close-open—16$^3/_4$-cycle close duty cycle

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4. Quick Acting Release Latches, **Carl Thuman.** *Product Engineering* (New York, N. Y.), volume 14, number 11, November 1943, pages 740–42.

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Discussion

Otto Naef (American Gas and Electric Service Corporation, New York, N. Y.): This new 3-cycle operating mechanism is built around simple, sturdy elements well proved in earlier mechanism designs. It retains the mechanical trip-free feature without any compromise. Mechanism linkage, latches, solenoid trip magnet, and control valves are of conventional design. These elements have been adapted to high-speed tripping and reclosing duty by the addition of a lightweight secondary latch, a high-speed cutout switch for the control valve, and a rather elaborate arrangement for exhausting the air from below the driving piston.

Mechanical trip-free action has the advantage of dissociating the opening of the circuit breaker from the retarding effects of back pressure, inertia, and mechanical friction of the pneumatic piston. If accomplished without undue complication, as seems to be the case with this mechanism, it is a very desirable feature. Since the trip-free linkage must reset before a closing operation can be started after tripping, reclosing times are of necessity longer than with mechanisms which are only pneumatically trip free. That, in spite of this handicap, the first reclosing operation is completed after 14 cycles, and successive reclosures are possible in little more than 15 cycles, must be considered a remarkable achievement.

It has happened that operating mechanisms for circuit breakers failed to close fully under adverse conditions, such as extremely low temperatures, excessive friction, and so forth. The consequences of such failures have been the burning out of shunt resistors and internal flashovers to the tank, resulting in costly damage and prolonged outage. The users would like to see a safety device incorporated in the mechanism, returning it automatically and swiftly to the open position if latching does not occur within the normal closing time. It appears that it should not be difficult to attach such a device to the mechanism described in this paper.

L. J. Linde and **H. L. Peek:** The authors are pleased with Mr. Naef's evaluation of this new circuit-breaker operating mechanism. The emphasis he places on simplicity and conventional design confirms the designers' judgment in practicing basic and straightforward kinematics.

He recognizes the special high-speed cylinder dump construction as a rather elaborate arrangement, but credits this design for achieving successive circuit breaker reclosures in as little as 15-cycle intervals. For circuit breakers that do not require high-speed reclosing during successive operations it is quite practical to substitute a simple cylinder base and low-capacity dump valve for the special cylinder exhausting system described in the paper. This additional simplification can be made without sacrifice to the high-speed characteristics of the first reclosing operation and without disturbing the high-speed tripping characteristics. The mechanical trip-free operation of the circuit breaker must be credited with making this simplification possible.

Mr. Naef offers an original thought regarding the protection of the circuit breaker against improper closing operations. If adverse conditions were to affect the closing characteristics of this operating mechanism or any mechanism that is mechanically trip free, it is quite practical to provide a safety device that will trip the circuit breaker through the trip coil by a time-delay relay or by contacts that would recognize this improper operation of the operating mechanism.

There appears to be a number of methods available for obtaining such protection, the need for which will undoubtedly be established by the user of circuit breakers.

We would like to point out, however, that protection is provided by the closing toggle, giving increased force where increased closing load is encountered, and by the practice of setting the lockout pressure switch at a point sufficiently above the minimum closing pressure to provide for positive closing against adverse conditions. These safeguards have prevented such a failure during development tests which have simulated extreme field conditions.